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NOTES ON LATERAL-DIRECTIONAL PILOT INDUCED OSCILLATIONS.(U)
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NOTES ON LATERAL-DIRECTIONAL PILOT INDUCED OSCILLATIONS

Ralph H. Smith

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2800 Indian Ripple Road
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March 1982

Final Report for Period May 1979-May 1981

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This technical report has been reviewed and is approved for publication.

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REPORT DOCUMENTATION PAGE		READ INSTRUCTIONS BEFORE COMPLETING FORM
1. REPORT NUMBER AFWAL-TR-81-3090	2. GOVT ACCESSION NO. AD-A113 996	3. RECIPIENT'S CATALOG NUMBER
4. TITLE (and Subtitle) NOTES ON LATERAL-DIRECTIONAL PILOT INDUCED OSCILLATIONS		5. TYPE OF REPORT & PERIOD COVERED Final Report May 1979 - May 1981
		6. PERFORMING ORG. REPORT NUMBER
7. AUTHOR(s) Ralph H. Smith		8. CONTRACT OR GRANT NUMBER(s) F33615-79-C-3620
9. PERFORMING ORGANIZATION NAME AND ADDRESS SYSTEMS RESEARCH LABORATORIES, INC. 2800 Indian Ripple Road Dayton, Ohio 45440		10. PROGRAM ELEMENT, PROJECT, TASK AREA & WORK UNIT NUMBERS 62201F 2403 24030526
11. CONTROLLING OFFICE NAME AND ADDRESS FLIGHT DYNAMICS LABORATORY, AFWAL/FIGC, Wright Aeronautical Laboratories, Air Force Systems Command, Wright-Patterson AFB, Ohio 45433		12. REPORT DATE September 1981
		13. NUMBER OF PAGES 82
14. MONITORING AGENCY NAME & ADDRESS(if different from Controlling Office)		15. SECURITY CLASS. (of this report) Unclassified
		15a. DECLASSIFICATION/DOWNGRADING SCHEDULE

16. DISTRIBUTION STATEMENT (of this Report)

Approved for public release, distribution unlimited.

17. DISTRIBUTION STATEMENT (of the abstract entered in Block 20, if different from Report)

18. SUPPLEMENTARY NOTES

19. KEY WORDS (Continue on reverse side if necessary and identify by block number)

Handling qualities	MIL-F-8785C
Flying qualities	Pilot dynamics
Manual control	Human pilot
Man-machine dynamics	Pilot induced oscillations
Handling Qualities Specifications	PIO

20. ABSTRACT (Continue on reverse side if necessary and identify by block number)

A method is developed for the assessment of lateral-directional pilot induced oscillation (PIO) tendencies. The method is applicable regardless of the flight control system mechanization. The relations between linear and non-linear system effects on the probability or nature of PIO are discussed. In its simplest form, the method proposed for PIO assessment is very similar to methods currently in use. The principal difference is that quantitative methods are provided for the identification of specific frequencies at which

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appropriate stability criteria must be satisfied if PIO is not to be a possibility. Based upon analyses in this report, the single axis, bank angle PIO is believed to be the most commonly encountered lateral-directional PIO. The possibility for pitch only PIO in the longitudinal mode is discussed. The methods of this report are believed to apply to the study of longitudinal PIO.

FOREWORD

This report documents a study of the lateral-directional pilot induced oscillation (PIO) phenomenon. The work was performed under Air Force Contract No. F33615-79-C-3620, Project 2403, Flight Control, Work Unit 24030526. The sponsoring organization was AFWAL/FIGC. Mr. Frank L. George was the Air Force Project Engineer. The author acknowledges the support given to this effort by Mr. George and Mr. Brian W. Van Vliet, also of AFWAL/FIGC, who provided essential data and contacts with other Air Force organizations. The contributions of Mr. John Smith, NASA DFRC, are hereby acknowledged. Mr. Smith graciously provided all of the YF-16 prototype data and developed some material specifically for the author's benefit. Finally, the assistance of Mr. John Hodgkinson, McDonnell-Douglas Aircraft Company, St. Louis, Missouri, and Mr. Rogers Smith, Mr. Robert Radford, and Mr. Randall Bailey of the Calspan Advanced Technology Center, Buffalo, New York, is acknowledged in obtaining necessary flight test data for the ESP in-flight simulation experiment.

TABLE OF CONTENTS

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LIST OF ILLUSTRATIONS

LIST OF ILLUSTRATIONS (continued)

LIST OF SYMBOLS

Symbol	Description
a_{n_p}	Aircraft normal acceleration measured at the pilot's station, positive up; ft/sec^2
a_{y_p}	Aircraft lateral acceleration measured at the pilot's station, positive right; ft/sec^2
j	$\sqrt{-1}$
K_c	Gain of the controlled element transfer function
K_I	Aileron-to-rudder interconnect ratio, δ_r/δ_a
K_p	Roll damper static gain, δ_a/p; degree/degree/second
or	
K_p	Gain of the servo model for the human pilot
K_r	Yaw damper static gain, δ_r/r; degree/degree/second
p	$\frac{d\phi}{dt}$, roll rate; degrees/second
PIO	Pilot induced oscillation
R	Pilot rating; Cooper-Harper scale unless noted
S	Average slope of $\lvert\phi/F_{AS}(j\omega)\rvert$ or $\lvert\phi/\delta_{AS}(j\omega)\rvert$ on $1 \le \omega \le 6$; decibels/octave
s	Laplace transform variable, $s = \sigma + j\omega$
T_R	Roll subsidence mode time constant; seconds
$Y_c(s)$	Controlled element transfer function
$Y_p(s)$	Servo model for the human pilot

Greek Symbols

Symbol	Description
$\Delta_{CL}(s)$	Closed loop denominator polynomial for lateral-directional aircraft dynamics with all flight control loops closed
δ_{AS}, δ_{LS}	Deflection of the pilot's lateral control stick, positive right; degrees or inches
δ_a	Aileron deflection, positive for resultant right roll; degrees

LIST OF SYMBOLS (continued)

Greek Symbols	Description
ζ_d	Damping ratio of the dutch roll mode
ζ_R	Damping ratio of dominant resonant aircraft dynamic mode; open or closed loop
θ	Pitch attitude; degrees
σ	Real part of s
τ	Time delay; seconds
ϕ	Bank angle; degrees
ω	Imaginary part of s
ω_c	Crossover frequency of pilot-aircraft system; radians/second
ω_{CL}	Closed loop frequency estimated from time response data; radians/second
ω_d	Dutch roll mode natural frequency; radians/second
ω_L	Limit cycle frequency; radians/second
ω_R	Natural frequency of dominant open loop resonant aircraft dynamic mode; radians/second

Transfer Function Notation

$\left(\frac{1}{T}\right)$	Shorthand notation for $\left[s + \left(\frac{1}{T}\right)\right]$
$[\zeta, \omega]$	Shorthand notation for $[s^2 + s\zeta\omega s + \omega^2]$
$\frac{\theta}{F_{ES}}(s)$	Pitch attitude to elevator stick force transfer function
$\Phi(j\omega_c)$	The normal acceleration phase parameter, $\angle \frac{a_{n_p}}{F_{ES}}(j\omega_c)$ $-14.3\ \omega_c$
$\frac{\phi}{\delta_{AS}}(s)$ or $\frac{\phi}{F_{AS}}(s)$	Bank angle to pilot's control transfer function

LIST OF SYMBOLS (continued)

Closed Loop Notation	Description
$\phi \rightarrow F_{AS}$ or $\phi \rightarrow \delta_{AS}$	The closed loop, piloted control of ϕ with F_{AS} or δ_{AS} considered as the pilot's output
$\theta \rightarrow F_{ES}$	The closed loop, piloted control of θ with F_{ES} considered as the pilot's output

SECTION I
INTRODUCTION

The work presented here continues the study of the pilot induced oscillation (PIO) phenomenon begun in Reference 1. The results presented in this report for the lateral-directional mode are complementary to, but not identical with, those for longitudinal dynamics.

Until much more data of quality can be obtained from flight test, it is probably not possible to determine the validity of these theories. In view of the complexities associated with the study of lateral-directional dynamics, it would be perhaps premature to suggest that this work represents a mature theory. Rather, it is offered for the assistance of others in the field who may have the interest and resources to test it in actual practice.

The method proposed in this report for the assessment of lateral-directional PIO appears to apply equally well to the longitudinal mode. To this author, this suggests that there may be an additional mode, not pursued in Reference 1, by which PIO can develop--specifically the "single loop" tracking of pitch attitude. It is conjectured that, for this attitude-only mode, normal acceleration dynamics are irrelevant, and that this mode probably only will be seen when time delay induces significant phase lag within the bandwidth of piloted control. This delay might be due to flight control system nonlinearities, digital flight control system delays, or equivalent delay due to higher order system dynamics.

The results of this study are very closely related to methods for PIO assessment that have existed for many years (e.g., Reference 2). For example, it has been said (Reference 3) that if "wide"-band control with a pure gain pilot model is possible, then PIO is unlikely. The present work supports that position and provides a quantitative definition of required system bandwidth.

The two principal contributions of this report and Reference 1 to the theory of PIO are:

1. To present a theory that can explain <u>why</u> the "primitive" pilot model form is appropriate for PIO study, and

2. To present a quantitative method for the prediction of pilot-aircraft system bandwidth sufficient for the assessment of PIO tendencies.

In the world of flight test applications, it is important to maintain an awareness of how difficult it is to validate any theory for PIO. There are no test methods currently available which can guarantee that PIO can be found when it is truly a potential problem, or that will ensure that PIO-free configurations will be identified as such. This author is not yet prepared to accept the notion currently in vogue that any airplane can have a PIO problem. Any airplane can be made to oscillate, certainly.

It is noted that there are many other factors of possible significance to PIO in addition to the aircraft flight control system dynamics addressed in this report. Most of these are considered in References 2 and 3. The work of Reference 1 and this report, however, is directed toward a study of aircraft dynamics in order to support the development of critical requirements for MIL-F-8785C. These generally are those which could affect the preliminary design process and enable the development of better tradeoffs between aerodynamic configuration and flight control system requirements.

The method for the assessment of PIO tendency is presented formally in Section IV. The estimation of system bandwidth (Section II) is considered to be crucial to the successful prediction of PIO (and to handling qualities, in general). The discussion of the "primitive" pilot model given in Section III is a restatement of previous discussions in References 1 and 4. This material is included here for the benefit of those who may wish to pursue the pilot modeling problem in future research efforts. Existing data are examined in Section V to evaluate the basic usefulness of the proposed PIO assessment method. The appendix contains a discussion of one of the important handling qualities data sets. These data are for longitudinal control in the approach and landing task. The discussion is included in this report because the data set was derived from a series of flight tests which were intended to search for PIO. These data were important to refining the estimation of closed loop bandwidth.

SECTION II

ESTIMATION OF PILOT-AIRCRAFT SYSTEM BANDWIDTH

The bandwidth of the pilot-aircraft system is the single most important parameter to the success of handling qualities prediction methods and to the quantitative analysis of PIO. The PIO theory of Reference 1 offered no convenient method for bandwidth prediction. It was suggested that, in order to apply the theory, available methods for pilot-vehicle system analysis could be used to estimate whether closed loop resonance was likely, and the resonant frequency. Because of the difficulty of accomplishing this in practice, the development of a recommended PIO design specification (published as an appendix in Reference 5) left the definition of system bandwidth purposely vague.

Reference 5 did offer a method for the estimation of pilot-vehicle system bandwidth in non-PIO conditions. This was the key to the method proposed for handling qualities assessment and prediction in Category A tracking tasks with Type IV aircraft. The method proposed was based on two observations:

1. During the process of selecting appropriate pilot models for the analysis of pre-PIO tracking dynamics in Reference 1, the Bode amplitude properties seemed to dominate the selection process; aircraft phase response appeared to be a secondary consideration. (It is recalled that this process is nothing more than the application of the "adjustment rules" from Reference 6 to estimate realistic parameters for the servo model description of the human pilot.)

2. The crossover frequency (bandwidth) data summarized in Reference 6 for single axis tracking suggest that crossover frequency is almost completely parameterized by the Bode amplitude properties of the controlled element (aircraft) dynamics. Whether this observation had been made by others is unknown; it was a revelation to this author.

Work subsequent to that of Reference 5 suggested that the crossover frequency predicted by the formula of Reference 5 was a good first approximation to the PIO frequency--particularly when PIO developed as the result of linear system dynamics. While nonlinearities in the flight control system, such as saturation, might affect the fully developed PIO frequency, the possibility exists that the initial PIO frequency may be equal to the crossover frequncy. In the development of design specifications, the properties of fully developed PIO (e.g., frequency and amplitude of the motion) are of no real interest. Only those conditions which precipitate PIO are of value to the development of criteria which can eliminate PIO as a problem. Thus, the crossover frequency, computed by the method of Reference 5, can be used as the resonant frequency for evaluation of the proposed PIO design criterion.

A. THE CROSSOVER FREQUENCY FORMULA

The basic relation for crossover frequency, ω_c, as a function of aircraft dynamics was derived in Reference 5 based on measurements of crossover frequency published in Reference 6. For pitch attitude control, it was determined that ω_c is parameterized by the average slope of the pitch attitude Bode amplitude response over the frequency interval of 2 to 6 radians/second. A limit, equal to 6 radians/second was imposed on ω_c.

Evaluation of the crossover frequency formula against other data sources suggests that the formula should be slightly modified as follows:

1. The slope of $|\theta/F_{ES}(j\omega)|$ should be averaged over 1 to 6 radians/second.

2. The restriction on the maximum value of ω_c should be removed.

The first modification brings the formula's predictions more in line with observations based on the LAHOS data (discussed in the appendix). The second modification was suggested based on discussions with Air Force Flight Test personnel regarding F-15 flight test data. The resulting formula is:

$$\boxed{\omega_c = 6.0 + 0.24\ S}\ ,\ \text{radians/second}$$

where S = average slope of $|\theta/F_{ES}(j\omega)|$, in decibels per octave, on the region $1 < \omega < 6$ radians/second.

The data of Reference 6 on which the formula is based were from a lateral control task, not unlike a roll control task. It is expected, therefore, that the formula will be suitable for applications to lateral-directional control problems. For lateral-directional control, the slope, S, will be the average slope of $|\phi/F_{AS}(j\omega)|$.

Throughout the remainder of this report, the slope, S, is computed as follows:

$$S = \frac{1}{5}\Big[A(2) - A(1) + A(3) - A(1.5) + A(4) - A(2) + A(5) - A(2.5) + A(6) - A(3)\Big]$$

where

$$A = |\theta/F_{ES}| \text{ in decibels, or } |\phi/F_{AS}(j\omega)|.$$

B. CONFIRMATION OF THE CROSSOVER FREQUENCY FORMULA

There are very little data available which are suitable for establishing the validity of the above formula for the roll tracking task. Two restricted data sets will be considered.

In Reference 7, Durand and Jex published estimates of the dominant closed loop natural frequency observed over short time intervals in a roll tracking experiment. Their values should approximate the crossover frequency and can be used to test the formula. The simulated roll dynamics were:

$$\frac{\phi}{\delta_{AS}}(s) = \frac{K_c}{s\,(s + \frac{1}{T_R})}$$

Three values of the roll subsidence time constant were tested. For one of these the pilot subject A was asked to track "normally," then intentionally use a high-gain technique, with a final reversion to the normal tracking technique. The average closed loop frequency observed in each case, ω_{CL}, is compared in the table below with the crossover frequency ω_c predicted using the above formula.

	Pilot Control Technique (Pilot A)				
	Normal	Normal	Hi-Gain	Revert-to-Normal	Normal
$\frac{1}{T_R}$	1.0	2.5	2.5	2.5	10.0
ω_{CL}	2.5	3.0	3.9	3.1	4.1
ω_c	3.34	3.8	3.8	3.8	4.43

It appears that ω_c, by formula, is generally larger than actual closed loop oscillation frequency, except for the worst case, high-gain condition where the agreement is very good.

In general, ω_c probably depends on the pilot. Durand and Jex showed that systematic variations in ω_{CL} occur, depending upon the pilot. The table below shows ω_{CL} obtained for each of four pilots. The average pilot rating, R, is also shown in each case (modified Cooper scale):

Pilot	R	ω_{CL} (rad/sec) Range	ω_{CL} (rad/sec) Median	Predicted R
D	2.25	2.3-3.4	2.8	3.65
A	2.35	3.0-3.2	3.1	3.75
B	2.50	3.1-3.8	3.4	4.15
C	3.75	4.0-4.4	4.3	4.75

The overall average $\omega_{CL} = 3.4$ is in very good agreement with the ω_c formula. In the table, the data are ordered according to pilot rating. Note that rating increases monotonically with frequency of loop closure. This supports the theory of Reference 5 for rating prediction (which concludes that rating increases with phase lag of the controlled element, *measured at the crossover frequency*). This variation is shown in Figure 1.

The ratings that would be predicted for each of these cases, using $\omega_c = \omega_{CL}$ and the pilot rating function from Reference 5 for pitch tracking, are shown in the table. These predicted ratings do not agree with the actual ratings. Based upon limited data, it appears that the ratings received in roll tracking are not so sensitive to controlled element phase as in the case of pitch control. Use of the rating function from Reference 5 for the prediction of roll handling qualities seems to yield conservative predictions. However, the Durand-Jex data do support the crossover frequency formula for applications to roll tracking.

An interesting, brief flight test of lateral-directional handling qualities was conducted as part of a larger test and reported in Reference 8. Smith et al. analyzed these data and supplied the author

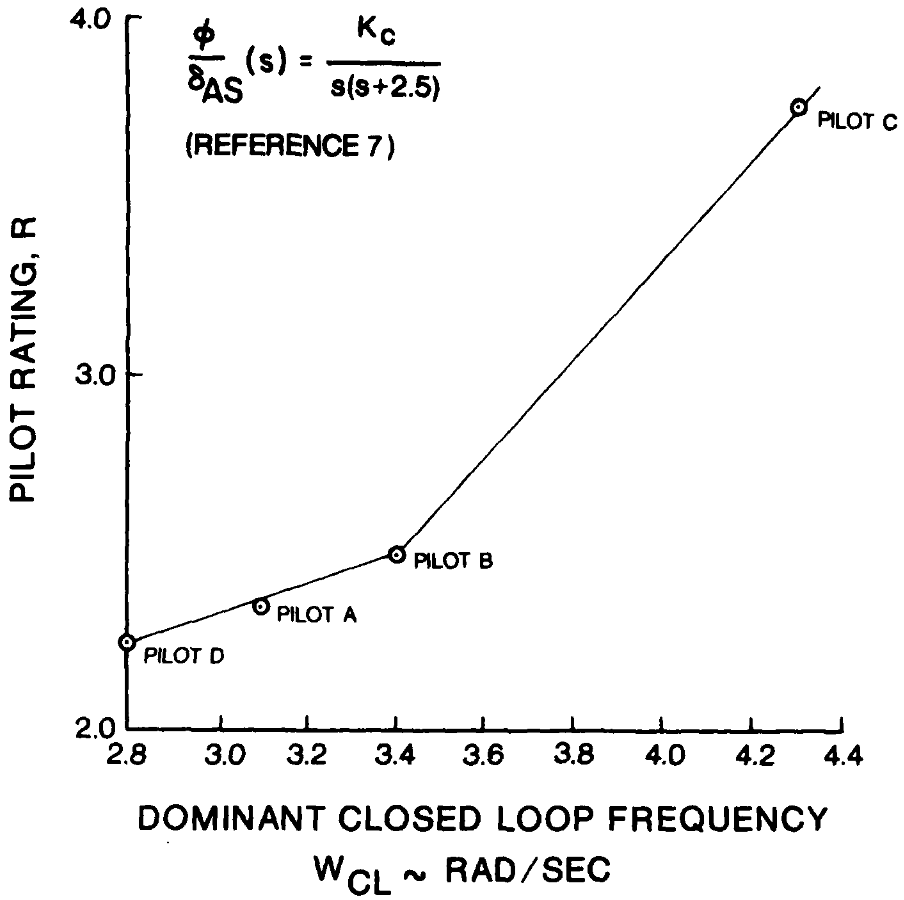


Figure 1. Closed Loop Frequency vs. Pilot Rating

with some of their preliminary results. These included response time histories and power spectral density (PSD) plots of lateral stick force for four configurations with which PIO was experienced in landing. These were roll-only PIOs to a good approximation; the dutch roll mode was canceled in the ϕ/F_{AS} transfer function.

The following table summarizes the pilot ratings, the PIO frequency (from the PSDs or time traces), and the crossover frequency ω_c determined by application of the formula given in Section IIA:

Config.	Cooper-Harper Rating	PIO Frequency (rad/sec)	
		Flight Test	Formula
L8	5/6/9	2.3	2.38
L11C	6/9	3.5-4.0*	3.58
L14B	5/7/10	1.7	1.99
L16A	8	3.5	3.22

*Estimated for pilot A, record 15 where PIO was a definite problem.

It appears that these data support the formula proposed for the estimation of system bandwidth.

One other case is worth noting since it isn't generally accessible in the open literature. For the Space Shuttle Orbiter, a mild longitudinal PIO was experienced on the fifth free-flight in the post-flare region of landing. Based on power spectral density data for free-flight 5, the PIO frequency (pitch mode) was about 3.5 radians/second. Using the Orbiter dynamics and the crossover frequency formula, it can be shown that $\omega_c \cong 3.8$. The agreement with flight test is, therefore, very good.

It is noted that the bandwidth formula indicates that pilot-aircraft system bandwidth will not be less in approach and landing than in, for example, Category A flight phases. This is an important result which should have significant impact on the character of design specifications for handling qualities in the landing task.

Other aircraft examples will be shown in Section V which further support the crossover frequency formula.

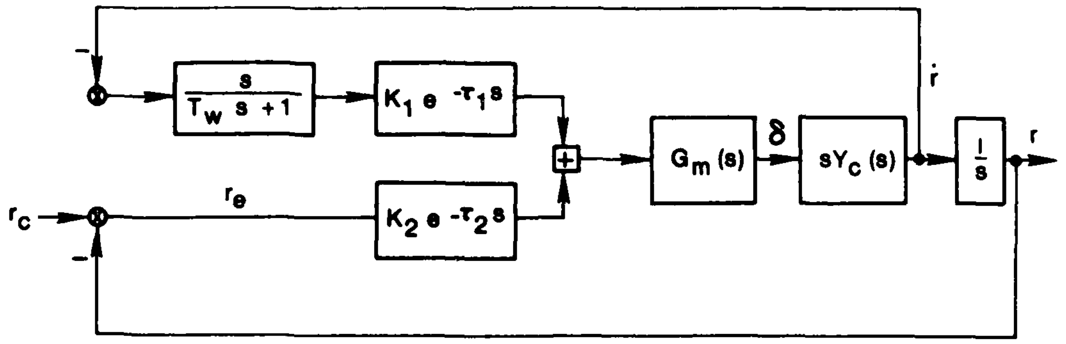
C. THEORETICAL MODELS FOR CROSSOVER FREQUENCY

The formula above for crossover frequency appears to be generally adequate for studies of aircraft handling qualities in attitude control. Despite its success with the limited testing it has received to date, the formula remains empirical.

To have genuine, widespread value to handling qualities analysis, it is important that the technique used for bandwidth prediction be applied across all conditions of task, aircraft dynamics, and external influences. This will require the development of a generalized model for closed loop bandwidth. This goal may be beyond our reach at the present time. Still, for purposes of discussion it may be of value to consider a very limited aspect of the theoretical problem.

In Reference 4 it was hypothesized that a pilot-aircraft model of the form shown in Figure 2 could possibly account for most of the trends found in the human pilot data base provided by Reference 6. It was speculated that if appropriate nonlinearities were included in the model--particularly in the rate control path--the model would apply, without parameter variations, over the entire range of controlled elements tested in Reference 6. These, however, were never found (although some tantalizing results were produced).

The version of the model shown in Figure 2 is intended to represent a linearization of the more complete, but abstract, model. When the inner (rate) loop is closed, the block diagram structure is identical to the single loop, unity gain feedback model generally assumed for the servo model description of the pilot-vehicle system. The resulting open loop transfer function $[r/r_e(s)]$ can be called $Y_p(s)\ Y_c(s)$, as is done with the servo description. When this is done, then $Y_p(s)$ becomes a function of $Y_c(s)$, the controlled element dynamics. The claim was made in Reference 4 that this functional behavior can account for the requirements imposed on parameterization of the servo model by the "adjustment rules" of Reference 6.



$Y_c(s)$ = controlled element transfer function

$$G_m(s) = \frac{\left(\frac{s}{30} + 1\right)e^{-.095s}}{\left(\frac{s}{10} + 1\right)\left[\left(\frac{s}{15}\right)^2 + \frac{2(.1)}{15} s + 1\right]}, \quad \text{neuromuscular system dynamics}$$

T_w = equivalent rate washout time constant

τ_1 = time delay, rate path

τ_2 = time delay, position path

K_1, K_2 = pilot gain constants, rate and position paths, respectively

Figure 2. First Order, Multiple Loop Model for Pilot-Aircraft System Dynamics

The purpose of discussing the multiple loop pilot model theory in this report is to demonstrate that there is probably a systematic, analytical technique that will enable the accurate a priori estimation of system bandwidth, ω_c, for conditions of general handling qualities interest.

Assume the following values for the pilot model parameters of Figure 2:

$K_1 = 1.0$	$K_2 = 7.0$	$1/T_w = .05$
$\tau_1 = .1$	$\tau_2 = .2$	

With these values fixed, the model of Figure 2 can be used to compute the open loop system's frequency response for any controlled element. Figures 3a, 3b, and 3c show the results for $Y_c(s) = K_c$, K_c/s, and K_c/s^2. Representative measured values of $Y_p(j\omega)\ Y_c(j\omega)$ from Reference 6 are shown for comparison. For clarity, the data variances from Reference 6 are not shown.

It is apparent that the prediction of crossover frequency for all three configurations is very good. The correlation between model and experiment is generally very good for $Y_c(s) = K_c$, and for $\omega < \omega_c$, K_c/s. For $Y_c(s) = K_c/s^2$, the phase angle correlation is poor. Better agreement could have been obtained by modifying the neuromuscular system parameters.

Unfortunately, the same model parameters do not work as well for the other $Y_c(s)$ tested in Reference 6 (tabulated on the following page). This is to be expected, since the model is but a crude approximation to that required by hypothesis.

Nevertheless, it is instructive to visualize how such a model might be used to explore handing qualities sensitivity to pilot technique. In the following table, the change in crossover frequency due to a 50 percent decrease in the pilot's rate gain is shown as a function of $Y_c(s)$. All other pilot model parameters are constant and equal to those used in Figures 3a, 3b, and 3c.

50 Percent Decrease in K_1	
$Y_c(s)$	Percent Change in ω_c
K_c	233% increase
K_c/s	10% increase
K_c/s^2	3% decrease
$K_c/(s-2)$	40% increase
$K_c/s(s-1.5)$	13% decrease
$K_c/(s-.425)$	0% change

If it were known how pilot parameters such as K_1 varied as a function of stress, g-loading, etc., then a general model for the estimation of ω_c would permit prediction of the resulting effect on handling qualities. This assumes that a parameter such as $\measuredangle\, \phi/F_{AS}(j\omega_c)$ has a one-to-one correspondence with pilot rating.

It is not sufficient to merely assume a specific change in ω_c and see what the effect on handling qualities would be. There are at least two sensitivities that must be jointly considered:

- the sensitivity of pilot model parameters to factors such as stress, muscle tension, external constraints, etc. (which invoke physiological considerations), and

- the sensitivity of system bandwidth to variations in pilot parameters (which is a function of aircraft dynamics).

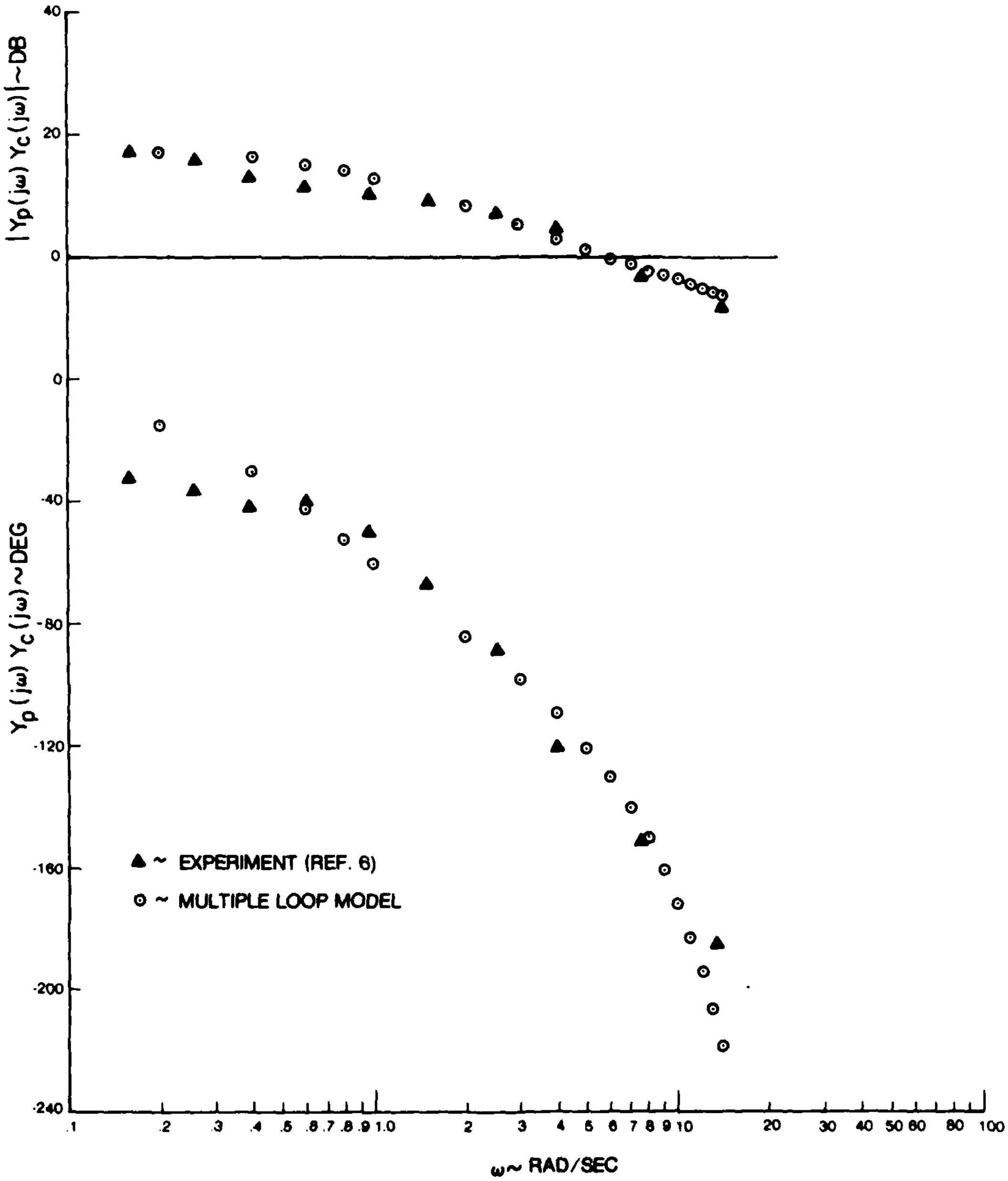


Figure 3a. Open Loop Pilot-Vehicle System Frequency Response; $Y_c(s) = K_c$

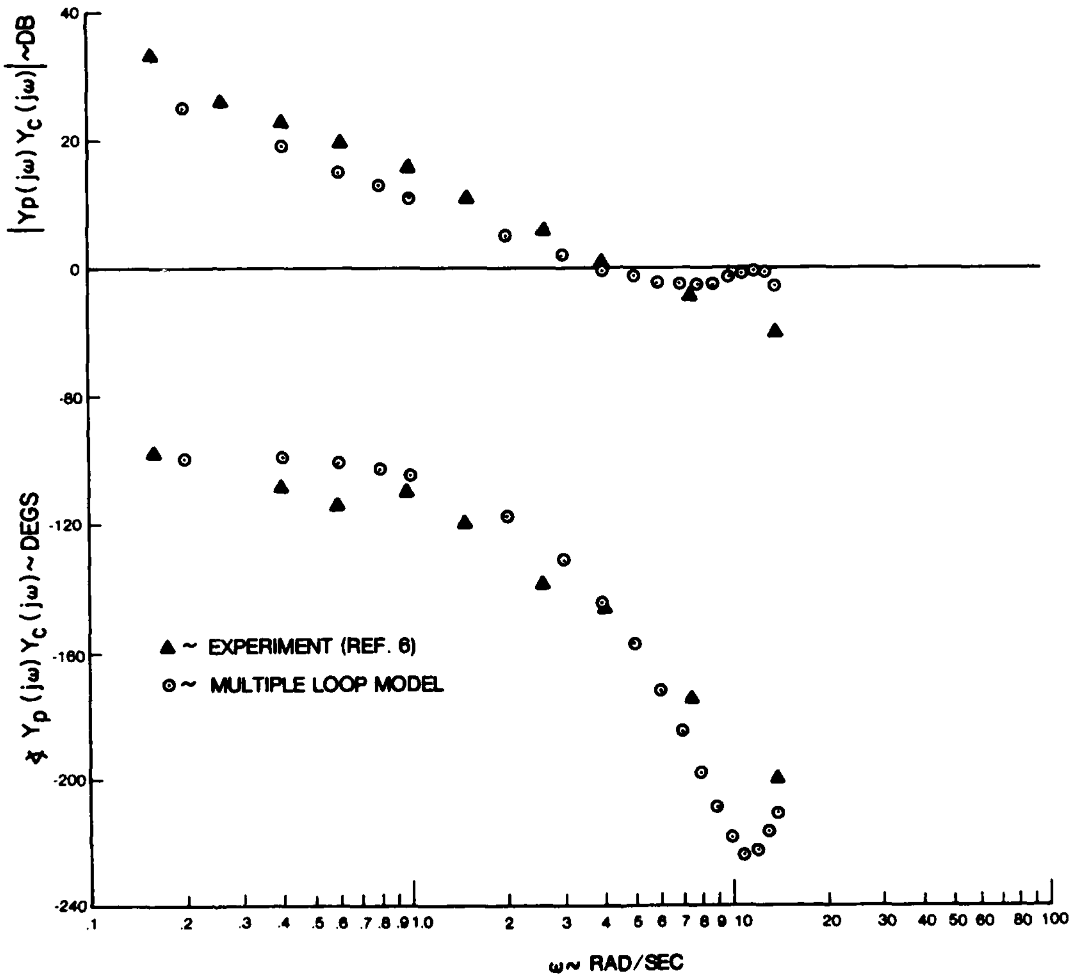


Figure 3b. Open Loop Pilot-Vehicle System Frequency Response; $Y_c(s) = K_c/s$

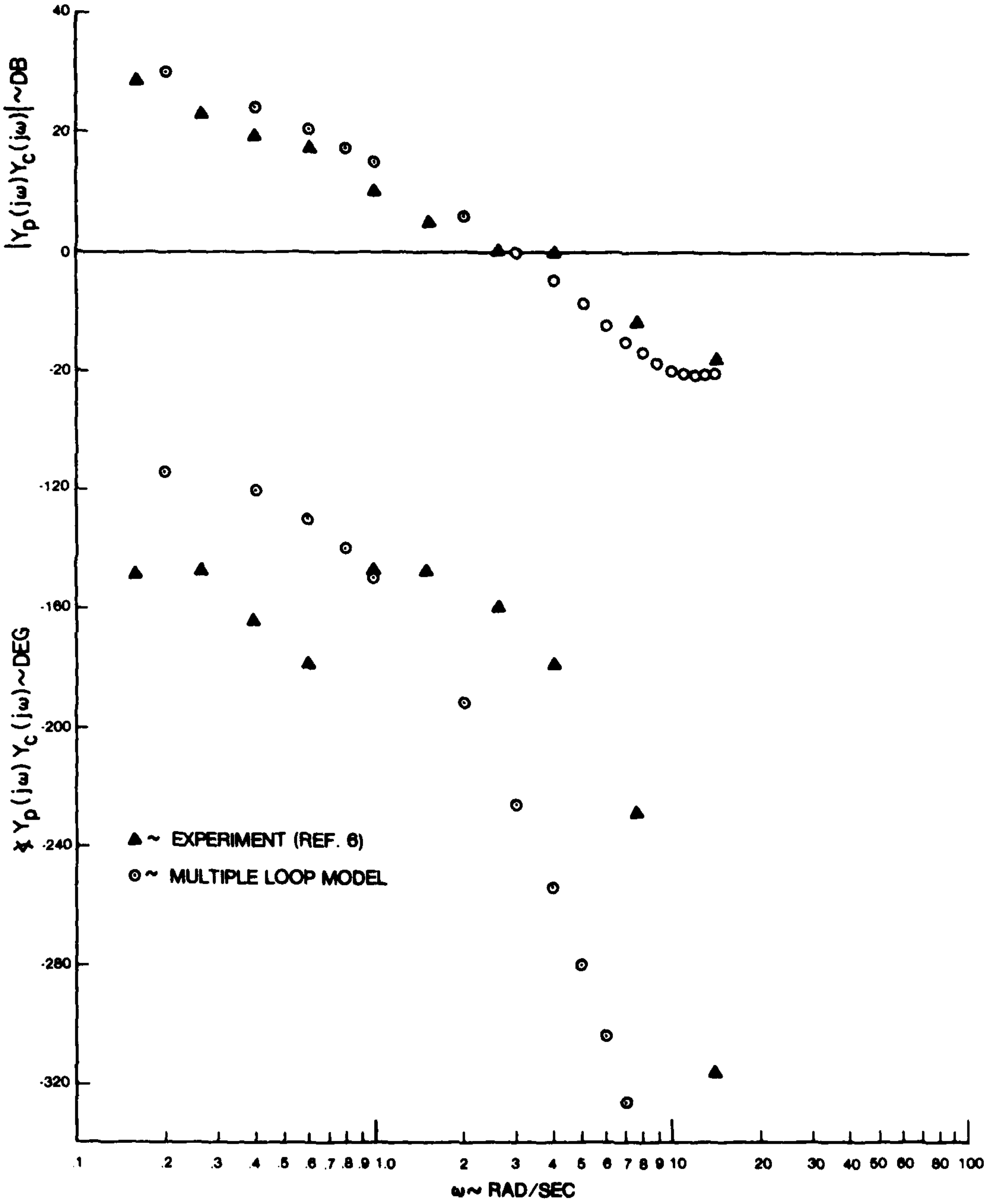


Figure 3c. Open Loop Pilot-Vehicle System Frequency Response; $Y_c(s) = K_c/s^2$

As a final comment, the sensitivity data tabulated previously support the qualitative arguments made in Reference 5 for the first three controlled elements. That is, ω_c is extremely sensitive to rate gain when $Y_c(s) = K_c$, not very sensitive for K_c/s, and almost independent of rate gain for K_c/s^2.

SECTION III
THE "PRIMITIVE PILOT"

The pure gain pilot model, often suggested and used in the PIO literature, was originally proposed because it seemed to be consistent with limited PIO case history data. Its origins, in other words, are empirical. Within the general context of the servo theory for human dynamics, the pure gain model appears to be out of place. There is, for example, no obvious way that it can be derived from the genral servo model through application of the adjustment rules of Reference 6. This connection between theory and practical observation is an important one to make. It is reasonable to expect that the degeneration of pilot model form should somehow reflect the physical mechanisms by which PIO can be initiated. If these can be understood, then there is hope that PIO can be eliminated from future designs.

One theoretical explanation which justifies the pure gain model for fully developed PIO was offered in Reference 1. It was noted there that the multiple loop pilot model (outlined in the previous section) appears to be structurally consistent with plausible PIO physical behavior and with the accompanying degeneration of pilot model form. In the multiple loop model, each mode of feedback control derives from an independent physiological sensor element. Thus, visually derived error rate is not treated as the time derivative of error since there exist different sensor dynamics, delays, and cerebral pathways. In the incipient PIO state if the pilot simply switches off all feedbacks except that which, to him, is most task critical, then the resulting pilot model is

$$Y_p(s) \cong K_p e^{-\tau s}$$

In other words, the "primitive pilot" model may be merely the forward-path pilot transfer function of the one remaining feedback. The concept of the primitive control is a cornerstone of the theory for longitudinal PIO in Reference 1 (where the remaining single loop was postulated to be normal acceleration). In Reference 1, available data suggested a nonzero time delay should be retained. It isn't clear whether this will also be true when the PIO is due to single axis pitch or roll tracking. Throughout the remainder of this report $\tau = 0$ will be assumed, for want of better information.

Following the postulates of Reference 1, it is assumed that the pilot regression to the primitive form of single mode control follows the development of substantial aircraft resonance in attitude response. This can result from open or closed loop inputs.

SECTION IV
A PROPOSED METHOD FOR PIO ASSESSMENT

As noted in the Introduction, the method proposed here for PIO assessment is not startlingly different from traditional methods (e.g., Reference 2). The essential difference is that a quantitative definition of required system bandwidth is offered. Also, the proposed assessment method attempts to establish a positive relation between the essential behavior of the real (nonlinear) flight control system and the idealized (linear) one.

The method proposed is confirmed in Section V against available case history data for lateral-directional PIO. There is no obvious reason to preclude the application to longitudinal dynamics, as well.

In the case of longitudinal PIO, it is suggested, but not proven, that PIO in pitch-only tracking is possible according to the proposed theory. It appears that this would require sufficient time delay (equivalent, due to higher order dynamics, or real) to make the $\theta \rightarrow F_{ES}$ single loop unstable at ω_c (from the formula). It is hypothesized that this would be true regardless of normal acceleration dynamics; if so, it would constitute another PIO mode distinct from that given by the theory of Reference 1. If this is true, then it should be possible to excite longitudinal PIO in fixed-base simulation provided sufficient delay exists in the θ/F_{ES} transfer function. This contradicts the author's conjecture in Reference 1 regarding PIO in fixed-base simulation. In all other respects, it is believed that the present theory complements that of Reference 1.

It should be noted that, in the original approach to the problem of understanding and predicting lateral-directional PIO, the author attempted to apply the longitudinal theory (Reference 1) with ϕ and a_{y_p} substituting for θ and a_{n_p}, respectively. For the data analyzed in this study, it appears that this approach is not satisfactory. Even for the M2-F2 PIO--which involves substantial dutch roll--the a_{y_p} dynamics appeared to add nothing to understanding the problem. In all cases, the $\phi \rightarrow F_{AS}$ loop dynamics were sufficient for PIO evaluation. The use of rudder pedal contributed to control difficulties with the M2-F2; it does not appear to have been the reason for the airplane's PIO encounters. Based upon undocumented PIO experiences with the B-52 and KC-135, this may be the usual involvement of rudder control in PIO when the controls are of the classical sort. We should, however, be prepared for "rudder"-only PIO when direct force control systems are introduced.

A. BACKGROUND

A Cooper-Harper scale rating of 8 represents a boundary between

(a) controllability not in question (R = 7), and

(b) controllability is conditionally dependent on pilot compensation (R = 9).

It is reasonable to expect that a rating of 8 might represent a threshold to PIO in those cases where the problem exists.

The relation derived in Reference 5 between Cooper-Harper rating and $\angle\, \theta/F_{ES}(j\omega_c)$ is shown in Figure 4. Note the knee in the relation at R = 8. The corresponding θ-phase is very close to -180 degrees.

This suggests that a simple necessary condition for attitude-only PIO, pitch, or roll would be that:

$$\angle\, \frac{\theta}{F_{ES}}(j\omega_c) \text{ or } \angle\, \frac{\phi}{F_{AS}}(j\omega_c) < -180 \text{ degrees}$$

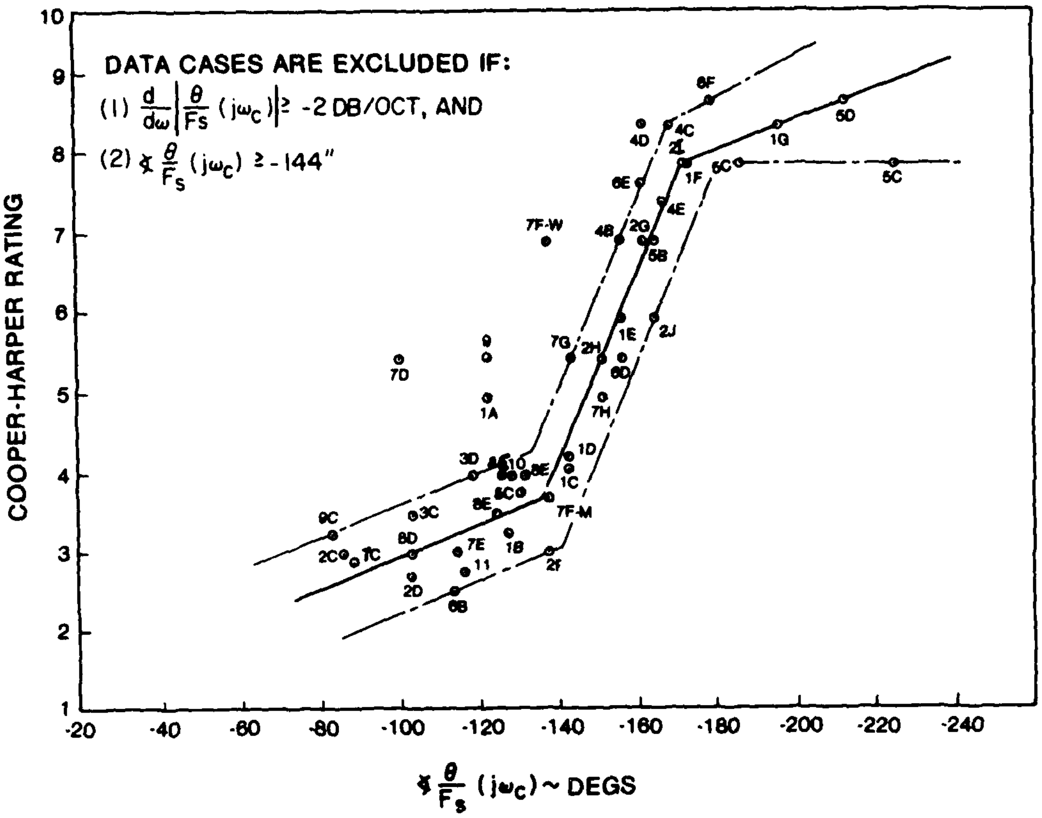
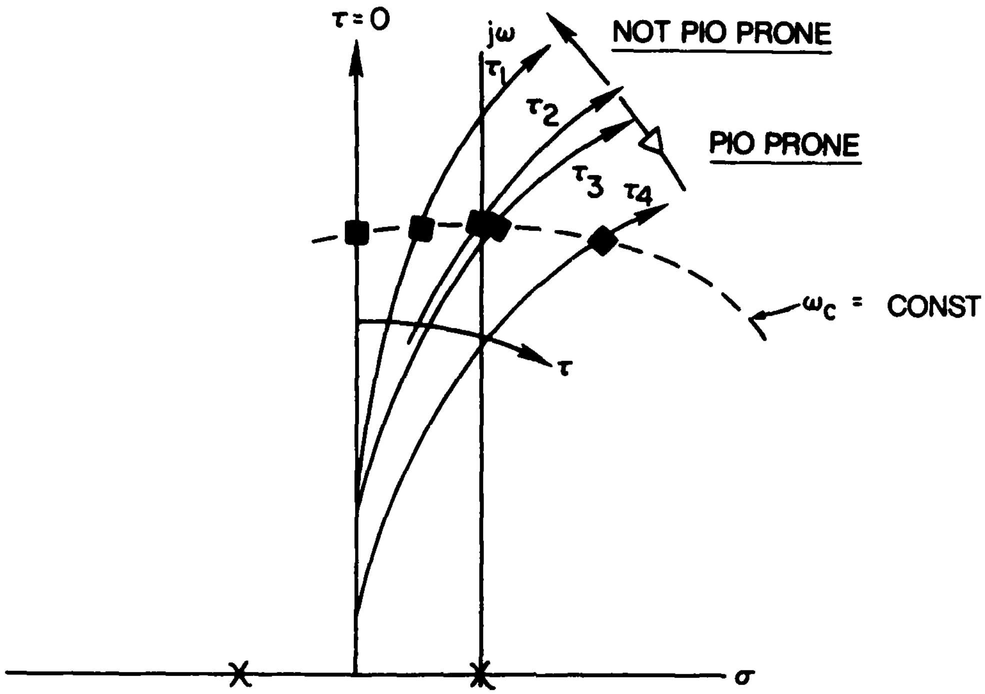


Figure 4. Cooper-Harper Rating vs. $\measuredangle\ \theta/F_{ES}\ (j\omega_c)$ (from Reference 5)

for linear system dynamics. Thus, one can use a pure gain pilot model and look for locus crossings of the imaginary axis on a root locus plot. When the frequncy of axis crossing is less than ω_c--by formula--then the crossing conditions represent a potential PIO state. In order to get PIO, something has to excite the pilot to adopt the primitive model form. The sketch following illustrates the relation between the frequency of axis crossing, ω_c and PIO in a roll tracking task without dutch roll. The effect of increasing time delay is illustrated. This could originate from within the flight control system and be an actual or equivalent delay.

Not· that ω_c is a function only of $|\theta/F_{ES}(j\omega)|$ or $|\phi/F_{AS}(j\omega)|$ which is unaffected by time delay. Thus, ω_c is a constant for all loci shown.



B. THE METHOD

It is possible to generalize the above remarks to include non-linear system dynamics. The resulting assessment method is best described by the flow chart of Figure 5.

Previously, the discussions of system dynamics have been restricted to closed loop dynamics. It is suggested that resonance in roll (or pitch) attitude due to open loop control or disturbance inputs, or to the use of secondary controls (e.g., rudder), must also be considered in a general PIO analysis, since this can lead to PIO initiation. For resonance induced by other than the $\Phi \to F_{AS}$ closed loop, it is proposed that

1. When the damping ratio of the dominant mode, $\zeta_R > 0.2$, then there is insufficient resonance to produce PIO.

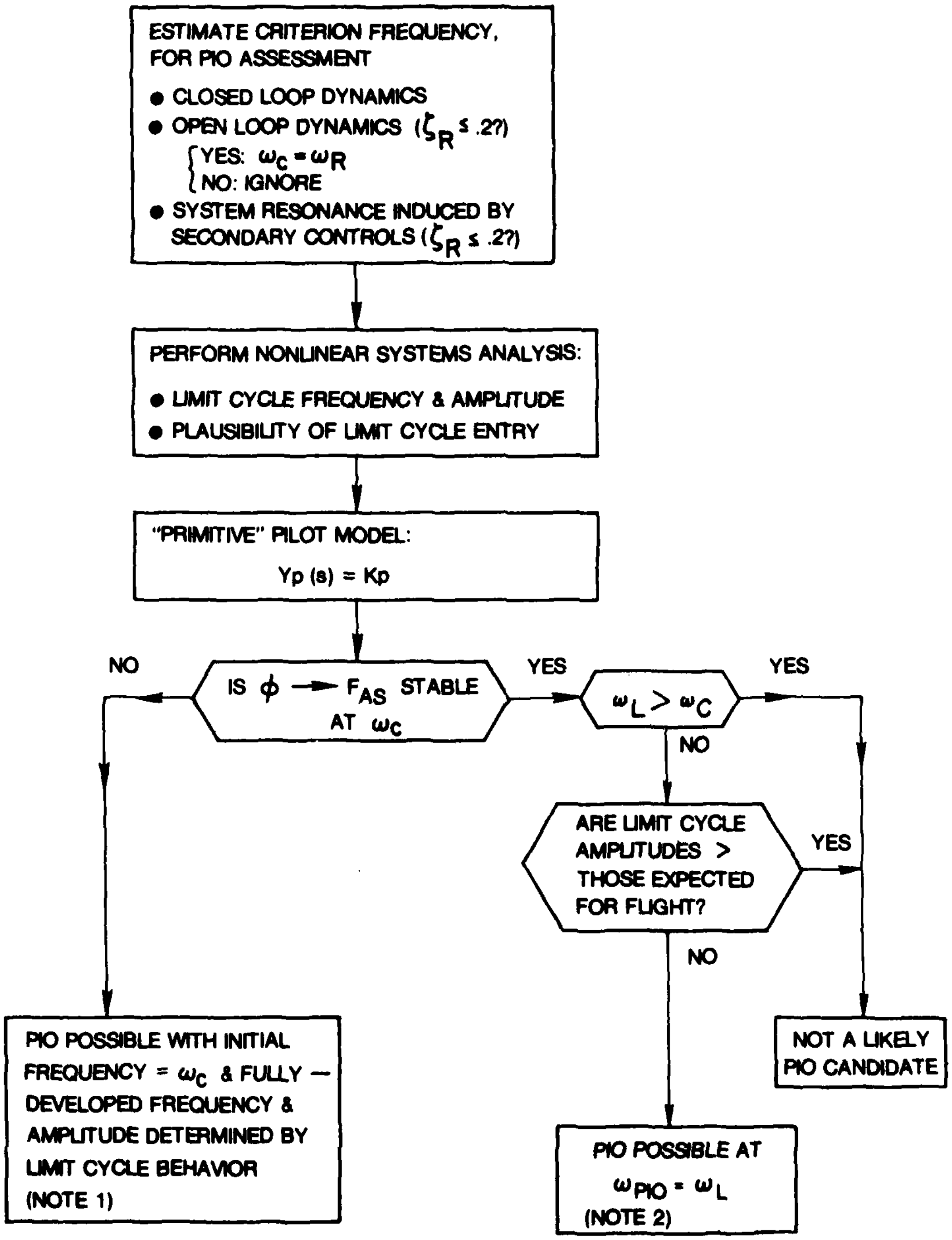


Figure 5. PIO Analysis Flow Chart

2. When $\zeta_R \leq 0.2$, then the criterion frequency for PIO analysis is equal to the resonant mode natural frequency; $\omega_c = \omega_R$.

The limit cycle analysis necessary to determine limit cycle frequencies, ω_L, and amplitudes may be accomplished in any manner desired. With very complex flight control systems, this will probably require a fairly sophisticated computer model for the aerodynamics plus control system.

There is necessarily room for art in applications of the method charted in Figure 5. It is, for example, all but impossible to be certain that limit cycles cannot occur for some input. Still, experience suggests that past PIO occurrences in which control saturation was a problem all could have been avoided if procedures such as that of Figure 5 had been used at the time of system design.

The following two notes apply to the figure:

Note 1:

The potentially catastrophic PIO which precipitates structural failure or pilot incapacitation would probably occur for this case when the system limits are exceeded prior to limiting due to flight control system (FCS) saturation or aerodynamic limits. The elimination of these PIOs will require fundamental revisions to the FCS or to the aerodynamic design.

Note 2:

Typically, expect that PIO resulting from this path will be large amplitude, low frequency in nature, and produce serious degradation of flight path stability. The elimination of these PIOs will generally require detailed revision to the FCS; in particular, saturation effects (rate or position) must be modified.

C. A CONJECTURE

It is probably important that the possibility of PIO due to use of direct sideforce control (DSFC or DFC) be considered within the context of Figure 5. This author knows of no PIOs of this nature that have been experienced in flight or simulation.

As an interim standard to prevent the occurrence of such PIO modes, it is suggested that a modification of the longitudinal PIO theory be used as follows:

1. Determine the principal outer loop cue for DSFC use. This may be the offset between a probe and drogue during aerial refueling, a HUD-derived cue for a ground attack mode, etc. Call this cue x.

2. Derive ω_c based on $x/F_{DFC}(s)$ where F_{DFC} is the pilot's force input to the direct force controller.

3. When $\angle x/F_{DFC}(j\omega_c) > -180$ degrees, consider that direct sideforce control probably won't produce PIO (barring serious nonlinearities).

4. When $\angle x/F_{DFC}(j\omega_c) < -180$ degrees, then if $\angle a_{y_p}/F_{DFC}(j\omega_c) < -180$ degrees, conclude that PIO is a possibility; otherwise, there is probably not a PIO problem.

SECTION V
CONFIRMATION WITH AVAILABLE CASE HISTORY DATA

In this section the PIO assessment methodology offered in Section IV will be tested for specific aircraft, aircraft dynamics simulated in flight, and for one fixed-base simulation. Only the lateral-directional modes are considered.

Complete nonlinear systems analyses will not be accomplished as part of this validation study. Detailed models would be required for critical system nonlinearities; generally, the data required to construct these and confirm the resulting models are not available in the literature. In those cases where flight control system nonlinearities play a significant role in the development of PIO, other literature is consulted or a simplified treatment is used to illustrate the PIO assessment methodology.

A. *YF-16 FIRST FLIGHT*

Reference 9 contains an analysis of the first flight PIO of the YF-16. The YF-16 PIO was dramatic, sudden, and violent. It consisted primarily of large rolling motions with fully saturated controls. The PIO occurred during what was supposed to have been a high speed taxi test to evaluate lateral control effectiveness. An overspeed condition occurred and the aircraft became airborne, followed shortly by uncontrolled PIO. As a safety measure the pilot elected to add power, gain altitude, and fly, rather than attempt immediate touchdown. During the PIO, light ground contact was made between the right horizontal tail tip and the left dummy AIM-9. The PIO time history is given in Figure 6. In addition to the data published in Reference 9, John Smith supplied to the author the ϕ/F_{AS} frequency response (take-off condition) shown in Figure 7.

From Figure 6 it appears that the frequency of wing-rock in the preliftoff period is nearly 5.0 radians/second. This was conceivably a

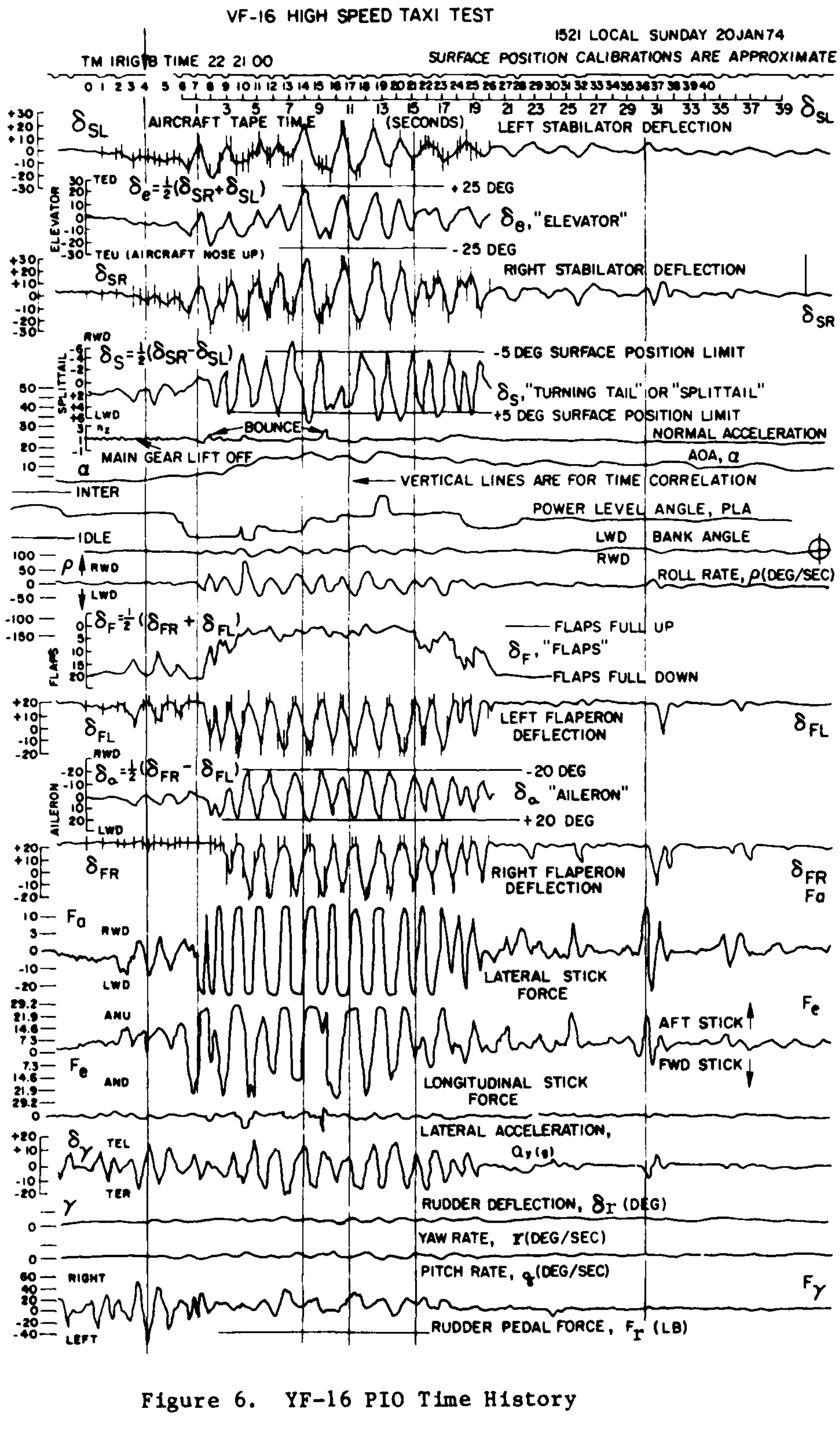


Figure 6. YF-16 PIO Time History

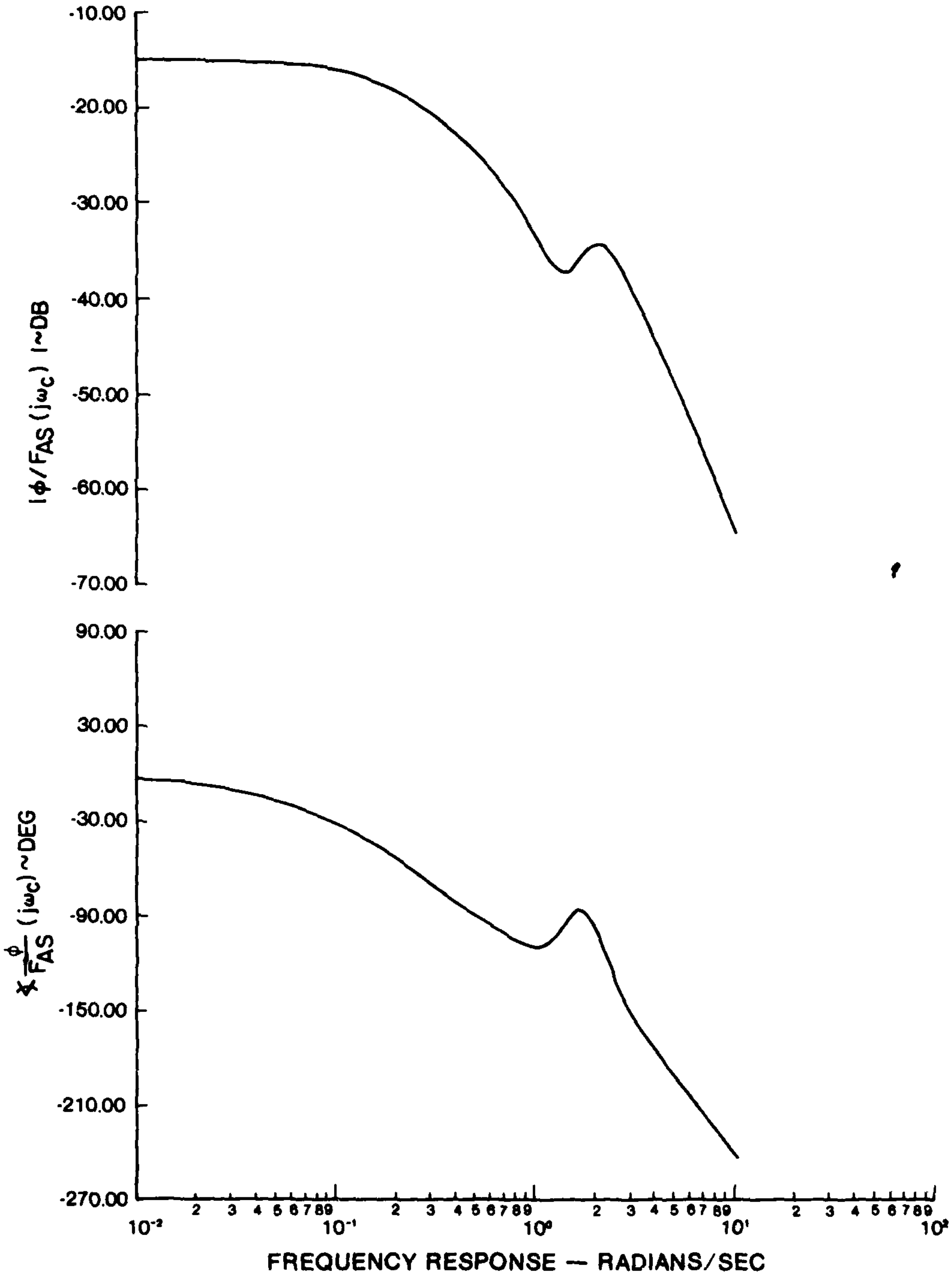


Figure 7. YF-16 Prototype; $\phi/F_{AS}(j\omega)$

factor in PIO initiation. It is presumed that the wing-rock resulted from open loop control inputs from the pilot.

At main gear lift-off followed by left wing drop, aileron stick force F_{AS} is approximately proportional to $\phi(t-\tau)$. Following control surface rate and position saturation, F_{AS} becomes approximately proportional to roll rate with no, or small, delay. This shift in apparent feedback cue architecture occurs at about $t = 4$ seconds, aircraft tape time. For $4 < t < 19$, $F_{AS}(t)$ remains approximately proportional to $p(t)$.

In the linear region of the flight control system and following relaxation of stick force in the post-PIO region, the dominant closed loop frequency is nearly 1 Hz. In the fully developed PIO, there are two observable frequencies:

1. When the control surfaces do not appear to be position saturated, the PIO frequency is approximately 5 radians/second.

2. When the control surfaces are position saturated, the frequency is approximately 3.6 radians/second.

Sudden jumps in closed loop frequency of the types described are probably typical of large amplitude, nonlinear motions with full authority fly-by-wire control systems. However, this will also occur with conventional stability augmentation systems (SAS) when limits of rate or position are exceeded; the YF-12 PIO is a good example of this (Reference 1).

The crossover frequency may be determined by formula from the ϕ/F_{AS} Bode plot of Figure 7:

$\omega_c = 4.18$

$\measuredangle \frac{\phi}{F_{AS}} (j\omega_c) = -186$ degrees

Then following the PIO assessment flow chart (Figure 5), it is seen that the $\phi \rightarrow F_{AS}$ closed loop is potentially unstable. The fully developed PIO frequency and amplitude are, therefore, determined by limit cycle states. Reference 9 indicates that stable limit cycle frequencies $3.3 \leq \omega_L \leq 4$ are possible with the pure gain pilot model.

Since $\measuredangle \phi/F_{AS}(j\omega_c)$ is close to the boundary value of -180 degrees, it would be prudent to evaluate the case where $\phi \rightarrow F_{AS}$ is stable at ω_c. Following the path indicated in Figure 5, see that ω_L is probably not greater than ω_c, using the results of Reference 9. In either case, it is, therefore, concluded that a large amplitude PIO is possible at frequencies between $\omega_L = 3.3$ and $\omega_c = 4.18$.

Using the same procedure, the modified flight control system (as defined in Reference 9) would be PIO-free provided the amplitude of F_{AS} (when F_{AS} is approximately sinusoidal) is less than about 14 pounds. It is impossible to say with complete assurance that this will always be the case. In comparison with the prototype system, it is nevertheless clear that PIO is much less likely with the modified system.

Observe that the present method of PIO assessment provides a <u>specific frequency estimate</u> for which closed loop stability is necessary if PIO is to be (probably) avoided. With real-world nonlinearities, it is very often not possible to eliminate the mathematical possibility that PIO can occur. The a priori estimate of required closed loop bandwidth provides a means of discriminating between the mathematically possible and the physically probable PIO states.

B. THE X-15

Taylor (Reference 10) analyzes a dutch roll PIO with the X-15 with SAS off. The PIO time history from Reference 10 is shown in Figure 8, and the roll rate frequency response is shown in Figure 9.

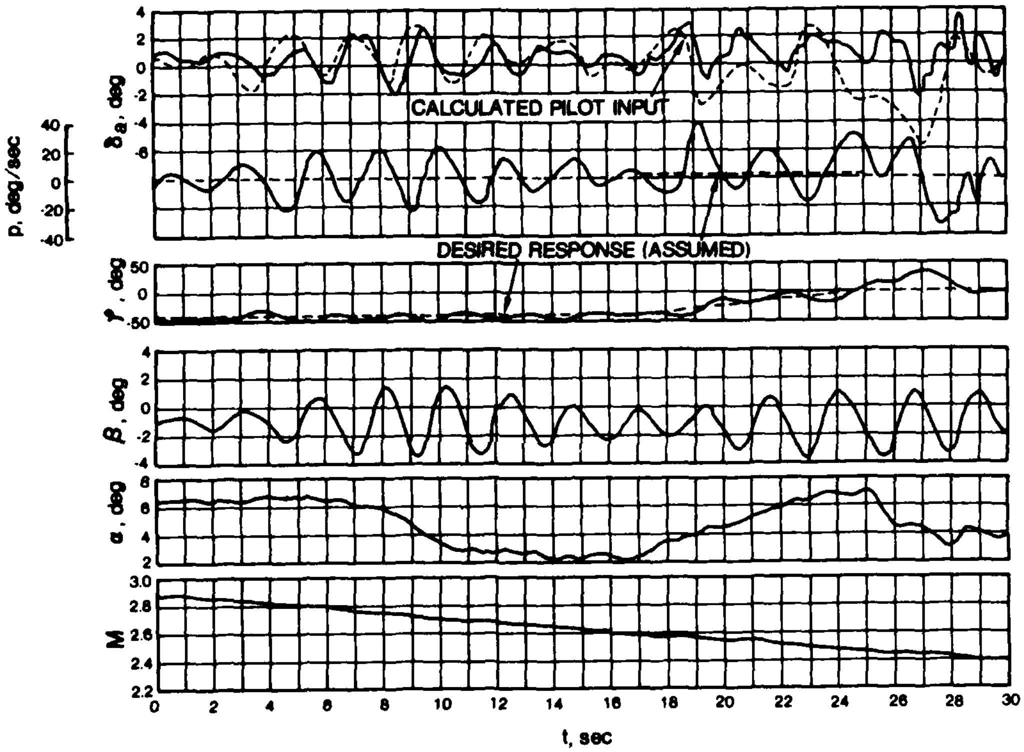


Figure 8. Time History of an X-15 Flight Near the Lateral Controllability Limit (from Taylor[10])

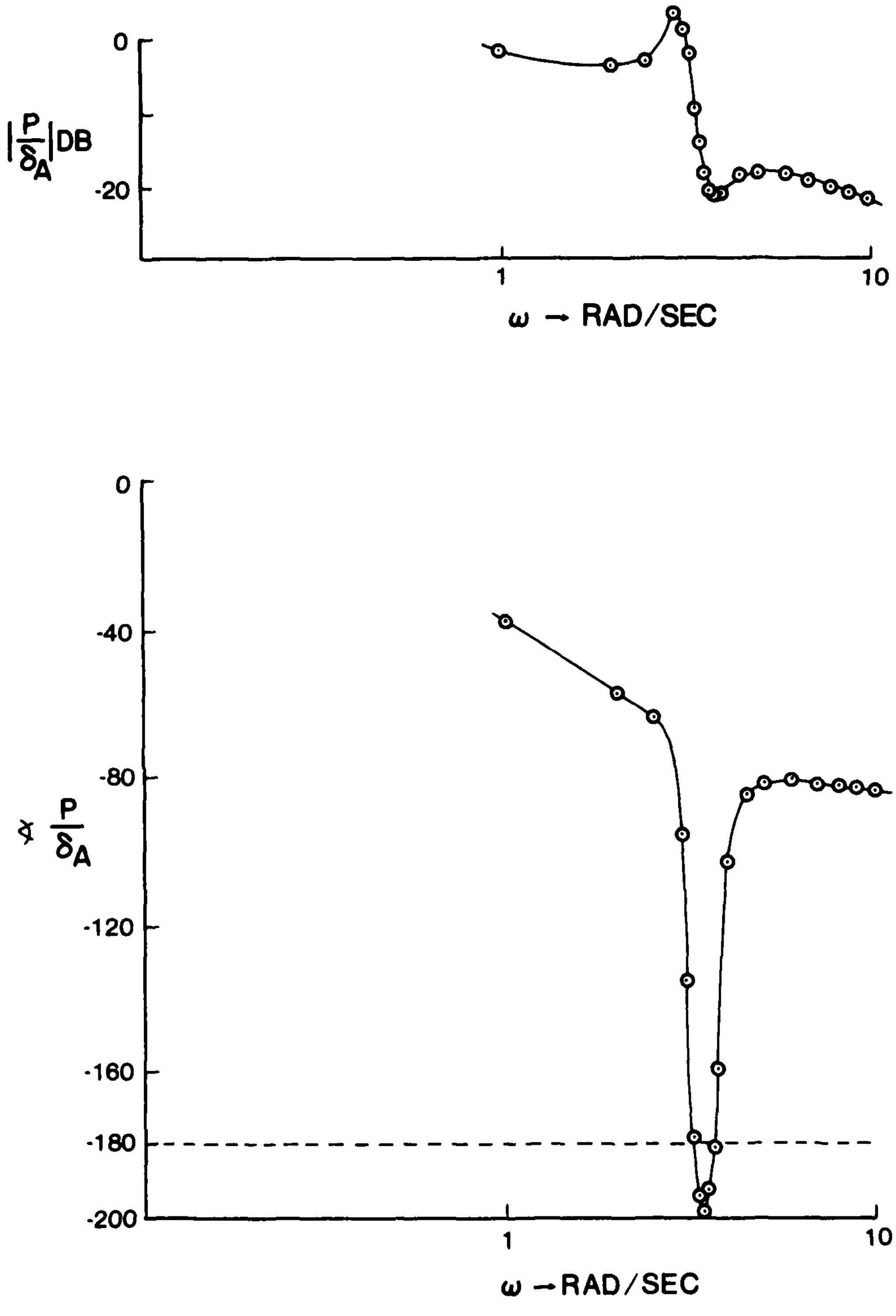


Figure 9. X-15 Roll Rate Dynamics

The airplane dynamics are considered to be linear in this flight condition. The rudder control was purposely not used.

First, examine the PIO tendencies predicted from closed loop dynamics. From Figure 9, compute

$$\omega_c = 2.15$$

$$\angle \frac{\phi}{\delta_a}(j\omega_c) = -150 \text{ degrees}$$

This indicates no PIO problem due to $\phi \rightarrow \delta_a$ dynamics, per se.

This situation changes when the open loop dynamics are considered. The dutch roll damping ratio and frequency are:

$$\zeta_d = .034$$
$$\omega_d = 3.11$$

Also, $\angle \phi/\delta_a(j\omega_d) = -227$ degrees. Thus, by the flow chart, $\zeta_d < .2$, and the possibility for PIO, therefore, exists at frequencies approximately equal to the dutch roll frequency. In fact, $\phi \rightarrow \zeta_a$ is unstable at ω_d and a PIO is, therefore, predicted.

Note that for this case $\omega_\phi/\omega_d > 1$. The closed loop instability could, in theory, be arrested with sufficient pilot gain. This would require a gain increase by the pilot of as much as 26 db (a factor of 19.953). This is not a realistic possibility.

C. THE M2-F2

Lateral-directional PIO problems with this aircraft occurred during three of 16 flights with three different pilots. These experiences are documented in Reference 11. The aerodynamic, mass, and inertial data used for the present report are from Reference 12.

The M2-F2 automatic flight control system consisted of a simple SAS with yaw rate and roll rate feedback through washout circuits. The SAS authority was ±5 degrees with actuator rate limits of 30 degrees per second in roll. Because of considerable adverse aileron yaw, the roll subsidence and roll spiral modes were coupled to produce a low frequency oscillatory mode. Aerodynamic nonlinearities destabilized this mode as the trim angle of attack approached 0 degrees. Because of this, an aileron-rudder interconnect (ARI) was used to counter the aileron yaw. The ARI ratio was pilot-selectable from the cockpit.

The SAS gains and the ARI ratio were different on each of the three flights for which PIO was obtained (flights 1, 10, and 16). Each of these three conditions, plus a condition for which PIO was definitely not a problem, will be examined below as a separate PIO case history. The M2-F3 will also be considered.

In view of remarks made elsewhere in this report, it is noteworthy that all PIO experiences with the M2-F2 were in flight phase Category B. It is shown here, however, that these may be diagnosed using criteria derived from Category A requirements.

M2-F2 Flight 1

The PIO time history for this case is shown in Figure 10 (from Reference 11). The required transfer functions were computed, using data of References 11 and 12, to be:

$$\Delta_{CL}(s) = .949\ (.591)\ [.498,\ .171]\ [.090,\ 4.585]\ (4.229)$$

$$\frac{\phi}{\delta_{AS}}(s) = .631\ (.572)(.586)\ [.688,\ 19.24]/\Delta_{CL}(s)\ \text{(deg/deg)}$$

$$\frac{r}{\delta_{RP}}(s) = -6.280\ (.511)(.587)\ [-.424,\ 1.128]\ (3.689)/\Delta_{CL}(s)$$
$$\text{(deg/sec/inch)}$$

$$\frac{\phi}{\delta_{RP}}(s) = 14.878\ (.571)(.571)(-5.303)(5.601)/\Delta_{CL}(s)$$
$$(\text{deg/deg})$$

The shorthand notation used here is:

$$\left(\frac{1}{T}\right) = s + \frac{1}{T}$$

$$\left[\zeta_n, \omega_n\right] = s^2 + 2\zeta_n\omega_n\ s + \omega_n^2$$

Note that in the following three figures, δ_{LS} is the lateral stick deflection denoted by δ_{AS} elsewhere in this report. This was done to preserve the notation of Reference 11.

From the bank angle transfer function, we can compute

$$\omega_c = 4.91, \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -246 \text{ degrees}$$

Also, a resonant mode exists at the open loop dutch roll:

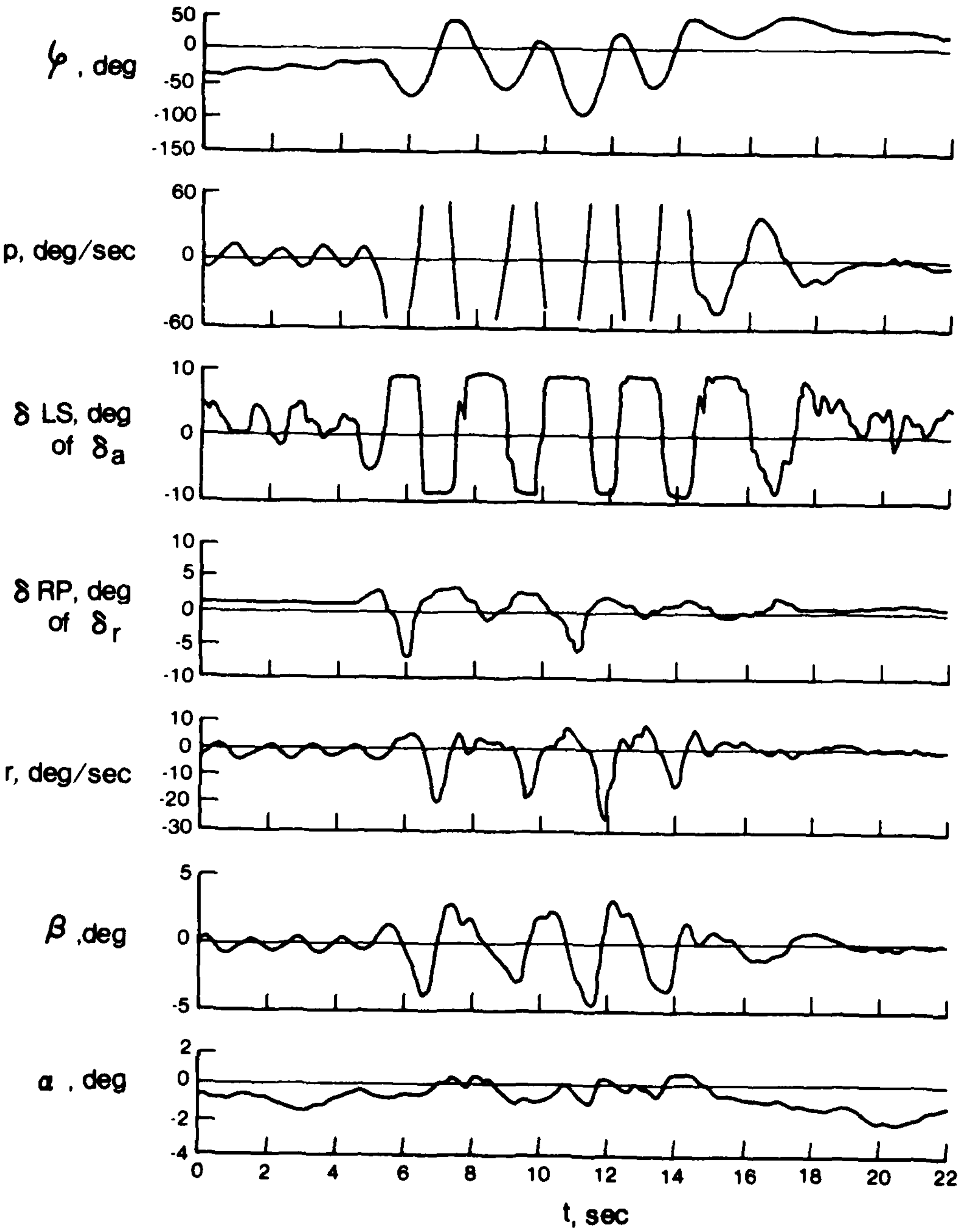
$$\zeta_d = .09 < .2$$

$$\omega_d = 4.585$$

$$\measuredangle \frac{\phi}{\delta_{AS}}(j\omega_d) = -211 \text{ degrees}$$

It is, therefore, concluded that PIO is possible with these dynamics. The initial PIO frequency will occur between 4.59 and 4.91 radians/second. Limit cycle properties will determine the resulting steady state PIO behavior. An example calculation of limit cycles is deferred until the discussion of flight 16.

The time history (Figure 10) indicates that $\omega_c \cong 5.2$ prior to PIO development. This is in good agreement with the predicted value.



Pilot's input and vehicle response

Figure 10. Time History of Pilot-Induced Lateral-Directional Oscillation on M2-F2 Flight 1. M = 0.48; h = 2830 m (9275 ft) to 1678 m (5500 ft); K_p = 0.6; K_r = 0.6 (from Reference 11, Figure 8)

Throughout the M2-F2 PIO occurrences, the pilots remarked about the tendency for rudder usage to promote loss of control. The r/δ_{RP} transfer function suggests that this is due in part to complex, non-minimum phase zeros at the low frequency. Any attempt by the pilot to control dutch-roll-induced yaw rate with rudder would produce an immediate loss of control with the dynamics shown. The time histories suggest, also, that rudder was used for control of p and ϕ. For the dynamics shown, the linear system with $\phi \rightarrow \delta_{RP}$ will be highly resonant at frequencies between about 4.1 and 4.6 radians/second. If the pilot attempts to correct bank errors at $\omega_c = 4.91$ with δ_{RP}, he will induce closed loop instability.

The M2-F2 Flight 10

Figure 11 shows the PIO time history for this case. The required transfer functions are:

$$\Delta_{CL}(s) = .970\ (.252)(.478)\ [.053,\ .889]\ [.856,\ 4.262]$$

$$\frac{\phi}{\delta_{AS}}(s) = 14.110\ (.571)(.665)\ [.516,\ 2.356]/\Delta_{CL}\ \text{(deg/deg)}$$

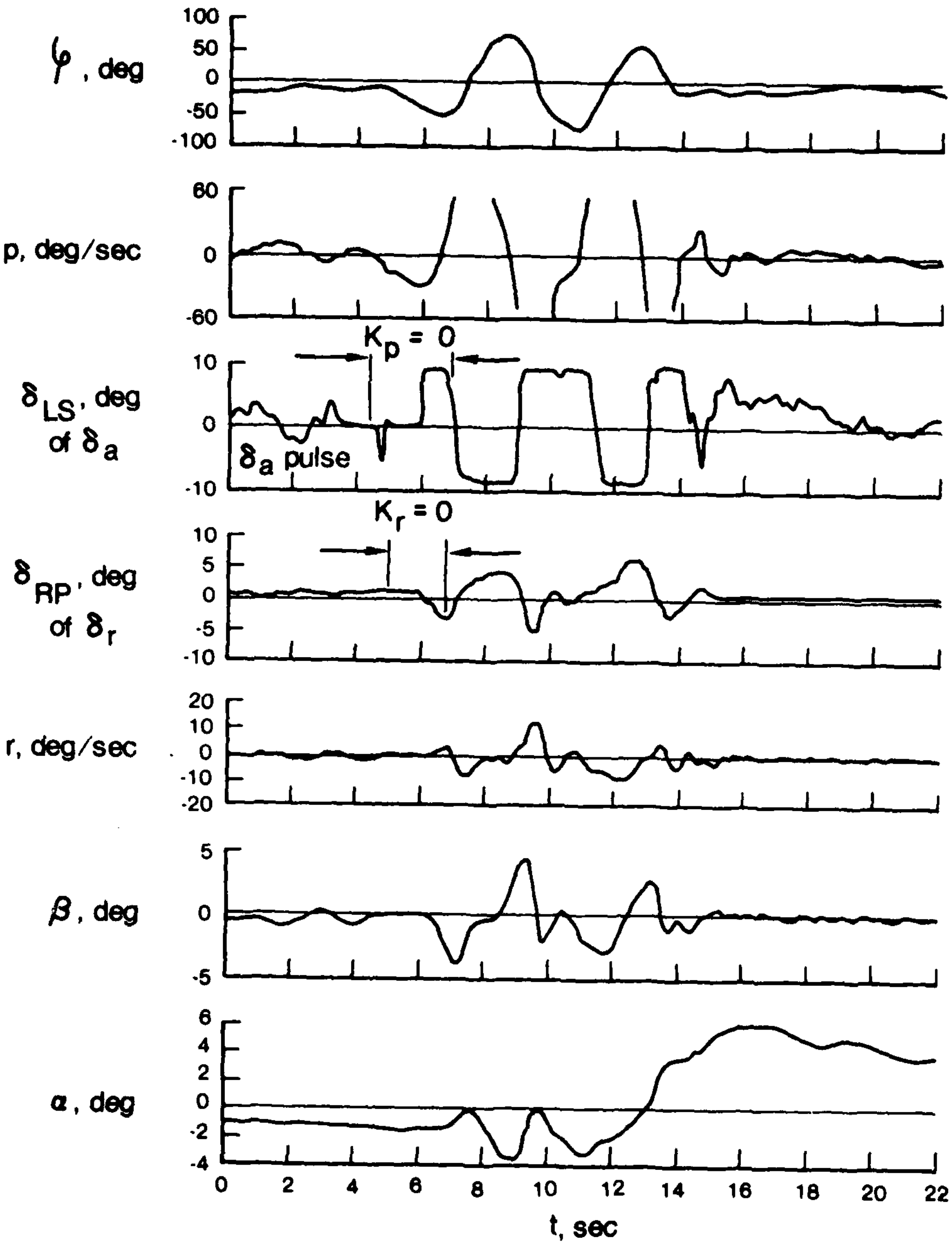
$$\frac{r}{\delta_{RP}}(s) = -5.230\ (.572)(.534)\ [-.626,\ 1.160]\ (5.210)/\Delta_{CL}\ \text{(deg/sec/deg)}$$

$$\frac{\phi}{\delta_{RP}}(s) = 8.805\ [1.000,\ .571]\ (-8.770)(8.665)/W_{CL}\ \text{(deg/deg)}$$

Compute:

$$\omega_c = 3.18$$

$$\measuredangle\ \frac{\phi}{\delta_{AS}}(j\omega_c) = -138\ \text{degrees}$$



Pilot's input and vehicle response

Figure 11. Time History of Pilot-Induced Lateral-Directional Oscillation on M2-F2 Flight 10. M = 0.61; h = 7020 m (23,000 ft) to 5800 m (19,000 ft); K_p = 0.4; K_r = 0.6 Except as Noted; K_I = 0.49 (from Reference 11, Figure 10)

To illustrate the method, let us assume that an analysis indicates a stable limit cycle at frequency $\omega_L = 1.53$ (the PIO frequency in the steady state condition). Then we would conclude that:

- The $\phi \rightarrow \delta_{AS}$ loop is stable in the linear range of flight control system operation.

- The predicted handling qualities (based on the Reference 5 method) are excellent.

- PIO is not a problem provided that the flight control system is not saturated.

- Any attempt to use rudder to stabilize dutch roll oscillations will result in loss of control.

But, according to Figure 5, since ω_c is greater than ω_L, there is a possibility that the limit cycle could be excited.

This was done (unintentionally) in flight 10 by turning off the SAS (see Figure 11) and applying an aileron pulse at t = 4.25 seconds. The aircraft was rolling left slightly when the aileron pulse was applied. As a result, it appeared to diverge. The pilot corrected with full right stick and quickly turned on the SAS. It appears that after hitting the stops he immediately applied rudder to aid in stopping the left roll. He appeared to continue to use rudder plus aileron to control roll angle; this resulted in closed loop instability, full saturation of the SAS, and entry into the limit cycle.

The aircraft mode corresponding to the coupled roll-spiral is resonant (damping ratio < .2). However, at the modal frequency, compute

$$\measuredangle \frac{\phi}{\delta_{AS}} = -146 \text{ degrees}$$

The linear system would be stable at this bandwidth. Since the frequency is probably less than possible limit cycle frequencies, then PIO due to stimulation of the coupled roll-spiral mode by any means is unlikely. (In an actual design study, it would be necessary to perform substantial analyses to verify what is merely assumed here).

Therefore, it may be concluded that, with a certain amount of ingenuity, the possibility of PIO could have been forecast. This is a particularly interesting example since the basic roll-to-aileron dynamics appear to be very good. The PIO occurred entirely due to flight control system saturation effects. With more control and SAS authority, there probably would have been no PIO with these dynamics.

M2-F2 Flight 16

Figure 12 shows the PIO time history for this case. The transfer functions of interest are:

$$\Delta_{CL}(s) = .949\ (.302)(.546)\ [-.173,\ .881]\ [.667,\ 4.219]$$

$$\frac{\phi}{\delta_{AS}}(s) = 13.886\ (.571)(.653)\ [.491,\ 1.909]/\Delta_{CL}(s)\ \text{(deg/deg)}$$

$$\frac{r}{\delta_{RP}}(s) = -4.962\ (.555)(.571)(3.999)\ [-.642,\ 1.378]/\Delta_{CL}(s)\ \text{(deg/sec/inch)}$$

$$\frac{\phi}{\delta_{RP}}(s) = 9.000\ (.571)(.572)(-8.191)(8.084)/\Delta_{CL}(s)\ \text{(deg/deg)}$$

Note that the coupled roll-spiral mode is unstable.

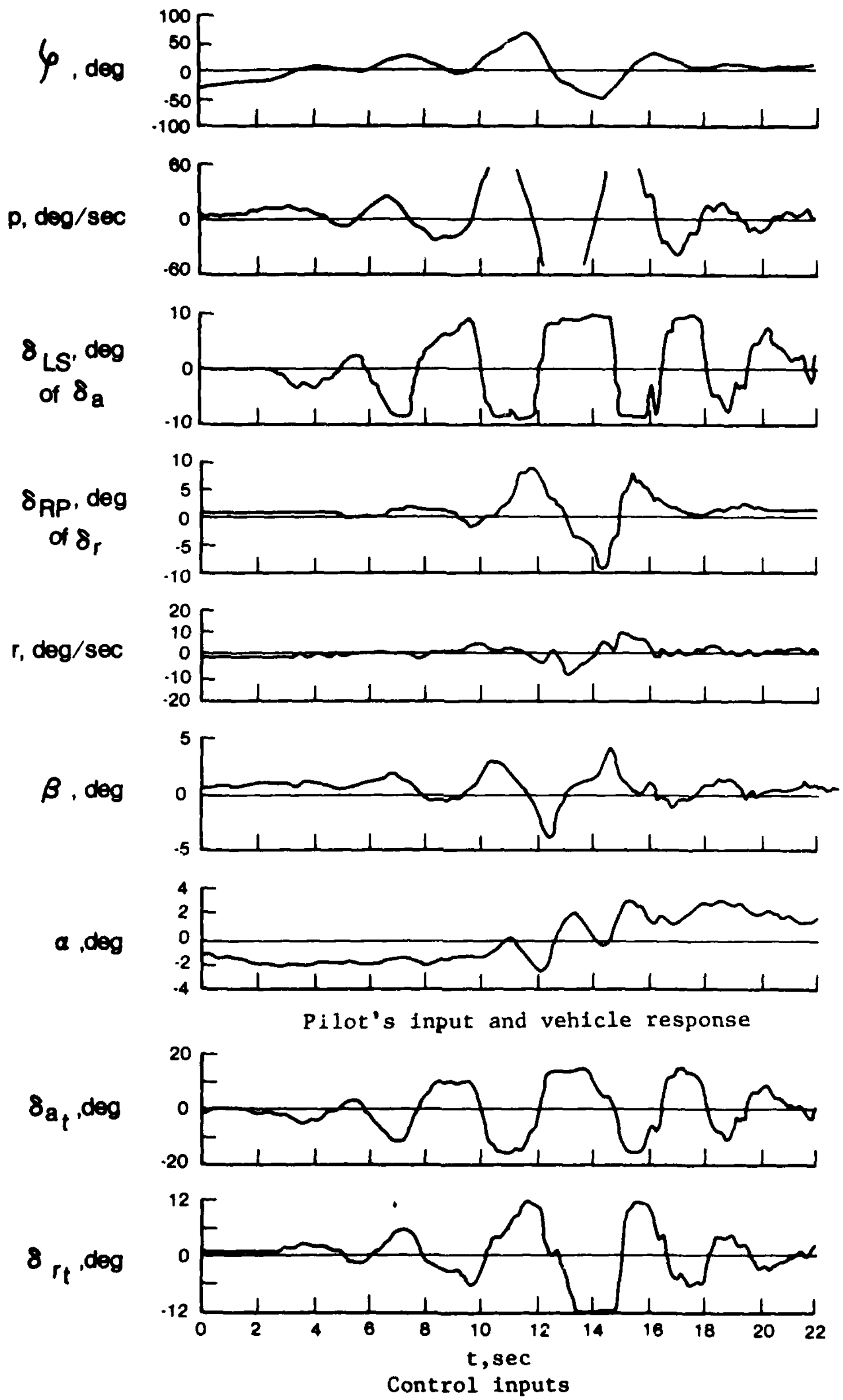


Figure 12. Time History of Pilot-Induced Lateral-Directional Oscillation on M2-F2 Flight 16. M = 0.48; h = 2620 m (8577 ft); V = 159.5 m/sec (523 ft/sec); q = 12,100 N/m^2 (253 lb/ft^2); K_p = 0.2; K_r = 0.4; K_I = 0.45 (from Reference 11, Figure 11)

Study of Figure 12 indicates:

- A (nearly) 2 Hz oscillation existed in aileron stick deflection in the pre-PIO condition. This appears to be pilot-generated and may originate from the dutch roll mode with high pilot gain.

- The PIO appears to really start at $t \cong 7$ following stick bottoming. The SAS doesn't appear to be saturated at that point. SAS rate saturation occurs at about 7.5 with SAS position saturation at $t \cong 10.5$.

- Rudder pedal is approximately proportional to bank angle throughout much of the PIO.

Compute:

$$\omega_c = 4.06, \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -127 \text{ degrees}$$

With this phase angle, the pilot rating predicted from use of Figure 4 indicates that pilot rating should be very good (about 3.5). There is no known way to predict the quantitative effect on rating due to the unstable roll-spiral mode. However, the time-to-double for the unstable mode is 4.55 seconds. This probably isn't short enough to seriously degrade the handling qualities. However, any periods of pilot inattention to bank angle control will result in roll oscillations at frequencies approximately equal to that at which the closed loop root locus originating at this mode crosses into the left-half plane. This frequency is 1.43 radians/second. The closed loop root locus of the linear $\phi \rightarrow \delta_{AS}$ system with a pure gain pilot is shown in Figure 13. The ϕ/δ_{AS} Bode is shown in Figure 14.

The manner in which the pilot's limited control authority could have contributed to the initiation of large amplitude PIO will be

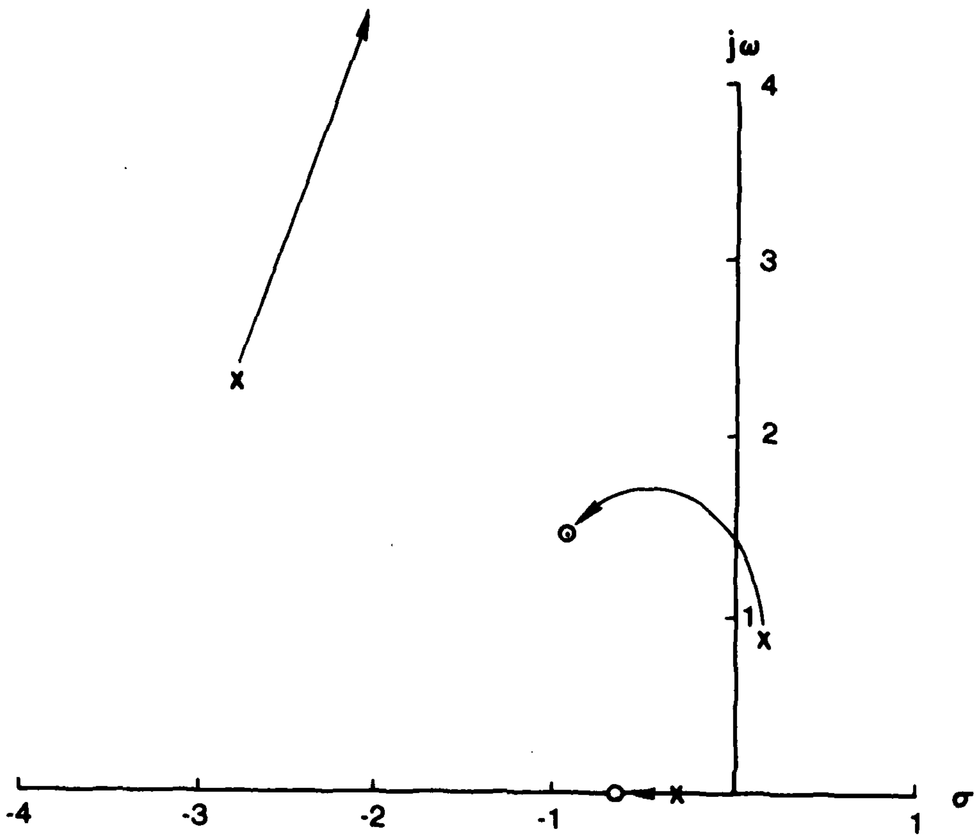


Figure 13. Locus of Pilot-Aircraft System Closed Loop Roots: $\phi \rightarrow \delta_{AS}$ (M2-F2 Flight 16)

illustrated with the following example. This is an oversimplification of the actual PIO mechanics since SAS saturation in position or rate isn't considered. The example does illustrate how SAS saturation may have been promoted even though, by all indications, the linear ϕ/δ_{AS} dynamics were very good.

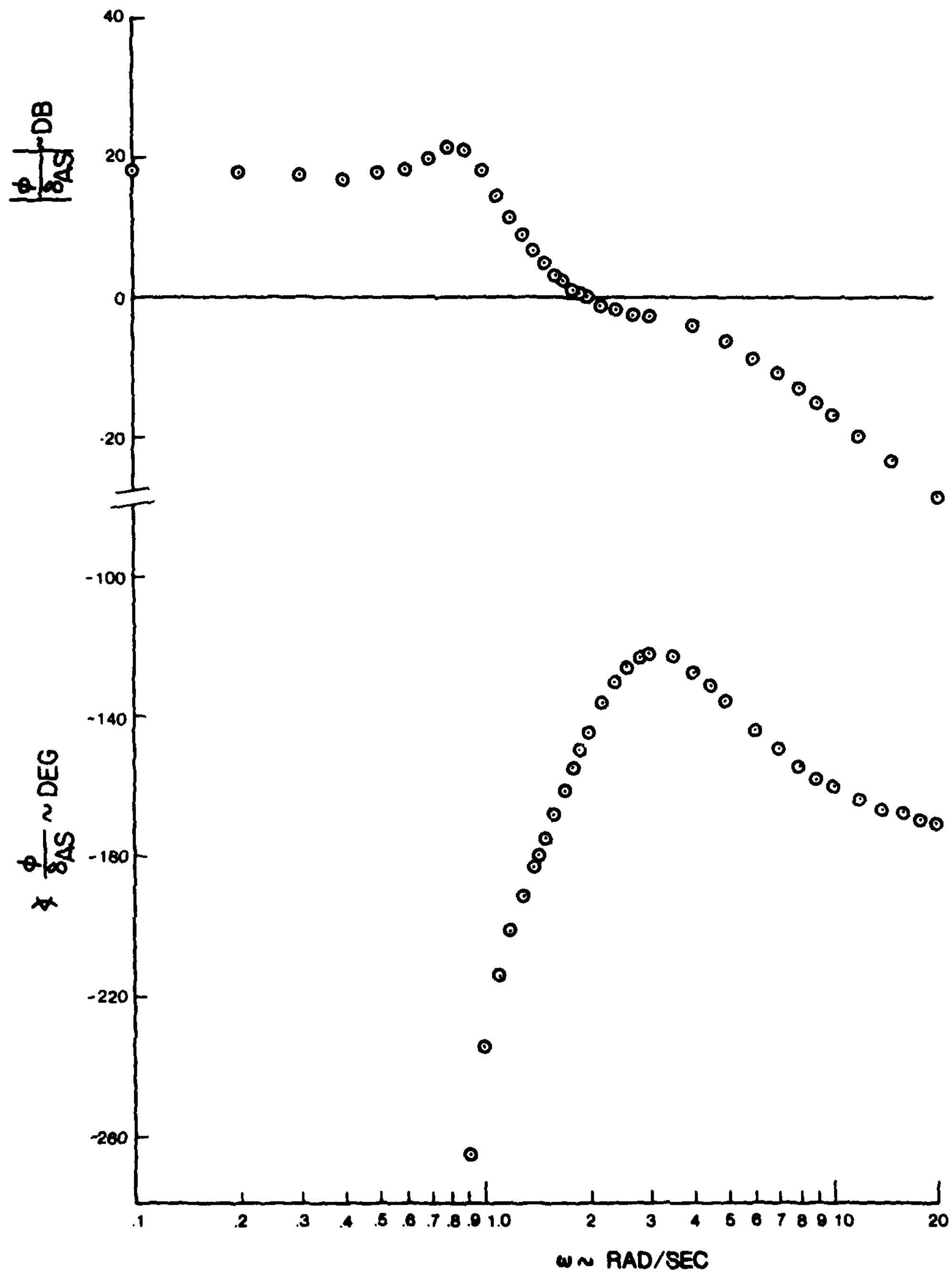
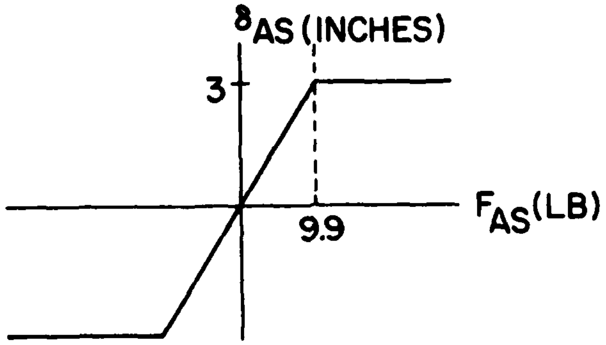
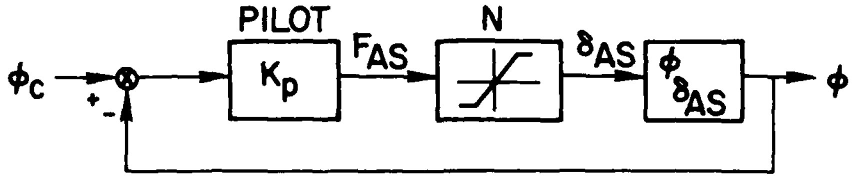


Figure 14. M2-F2 Flight 16 Dynamics: $\phi/\delta_{AS}(j\omega)$

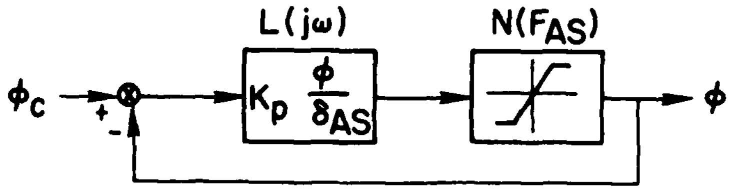
Assume that the variation between stick force, F_{AS}, and deflection, δ_{AS}, is linear except for symmetric δ_{AS} saturation as shown in the following sketch:



(In the aircraft transfer functions, δ_{AS} was expressed in degrees of δ_a.) The pilot-aircraft system model is as follows:



The linear elements may be combined to form the block $L(j\omega)$:



The necessary condition for a limit cycle is:

$$L(j\omega)\ N(F_{AS}) = -1$$

or

$$L(j\omega) = -\frac{1}{N(F_{AS})}$$

The sinusoidal describing function for simple saturation may be found in Reference 13. The two functions are plotted on the gain phase plot of Figure 15. The intersection of $L(j\omega)$ and $-1/N$ corresponds to a stable limit cycle with a frequency of 1.43 radians/second and a control stick force amplitude of 12 pounds. The linear function shown is plotted for a pilot gain of K_p = .135 pounds/degree; for other values $L(j\omega)$ is shifted vertically. For the simple saturation shown, the limit cycle frequency is constant. For a limit cycle to exist at all, an approximately periodic input control force of amplitude greater than about 10 pounds is required. Since the limit cycle is stable, saturated control oscillations at frequencies greater than ω_L = 1.43 will eventually decay to the steady state limit cycle condition.

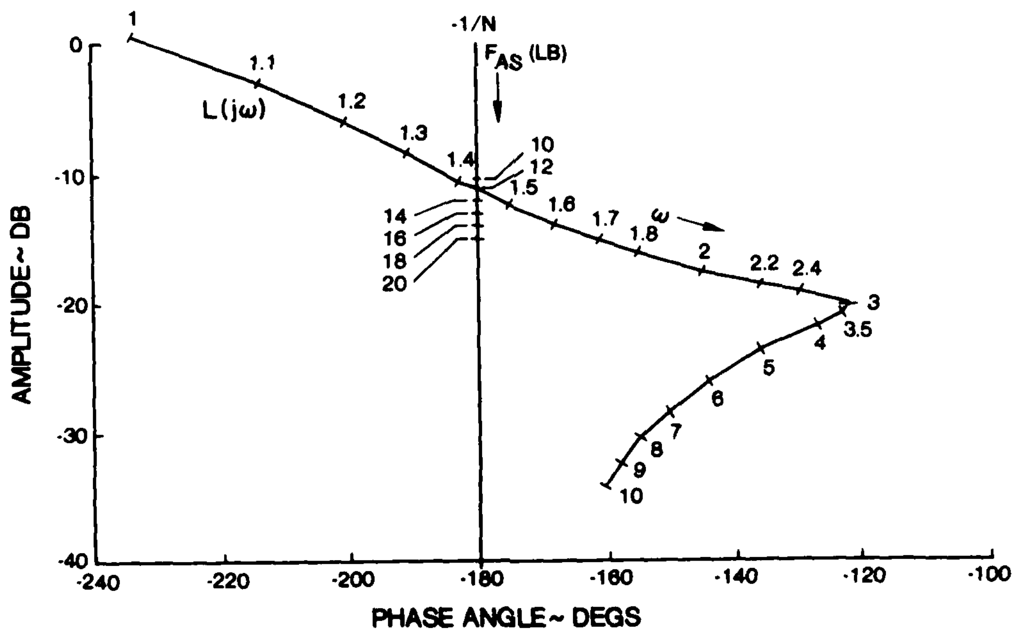


Figure 15. Gain Phase Plot: M2-F2 Flight 16 (Simple Saturation)

Now, consider the implications of these results to the assessment of PIO. Following the procedure of Figure 5:

1. The linear system's crossover frequency $\omega_c = 4.06$; this is greater than the limit cycle frequency $\omega_L = 1.43$.

2. The unstable roll-spiral mode almost guarantees that closed loop oscillations will be experienced in flight. Thus, periodic F_{AS} is inevitable.

3. The stick force required for saturation of stick deflection is only about 10 pounds. Full roll control yields only about 7 degrees/second of roll rate.

4. The sluggish roll response, combined with the likelihood that closed loop oscillations will occur originating from the roll-spiral mode suggests that the conditions required for the stable limit cycle will be obtained.

Consider the case where the limit cycle conditions are marginally met: $\omega_L = 1.43$ and F_{AS} amplitude = 10 pounds.

$$\left|\frac{\phi}{\delta_{AS}}\right| = 6.4 \text{ db} = 2.09 \text{ deg/deg}$$

The amplitude of δ_{AS}, referenced to δ_a, is 10 degrees (which corresponds to F_{AS} = 10 pounds). Then ϕ_{max} = 20.9 degrees and P_{max} = 30 degrees/second. The maximum increment of aileron deflection due to the roll rate feedback (gain = 0.2 degree/degree) is 6.0 degrees. Since the SAS authority limit is only 5 degrees, then it appears that saturation of δ_{AS} will also saturate the SAS. Thus, the limit cycle condition discussed above is an oversimplification; it illustrates the approach, however, which was the intent. In design practice, this would be sufficient information to warrant a design review. A possible remedy is to increase the pilot control authority; this, in fact, was done for the M2-F3.

The use of rudder to augment aileron roll authority would result in closed loop instability and SAS saturation.

It is believed that the above analyses are in reasonable agreement with the actual PIO experience on flight 16.

M2-F2 (Non-PIO Condition)

For this example, the aircraft configuration is identical to that of flight 16 except for the trim angle of attack. The value used was $\alpha_0 = 8.1$ degrees (it was -2.3 degrees for the flight 16 PIO condition).

For this case:

$$\Delta_{CL}(s) = .949\ (.581)(1.152)\ [.851,\ .222]\ [.202,\ 8.572]$$

$$\frac{\phi}{\delta_{AS}}(s) = 7.129\ (.571)(.583)\ [.207,\ 4.435]/\Delta_{CL}(s)\ \text{(deg/deg)}$$

$$\omega_c = 3.31$$

$$\measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -138 \text{ degrees}$$

Observe that the equivalent $|\omega_\phi/\omega_d| < 1$. Because of this, there are no resonant closed loop modes. The damping ratio for the dutch roll mode is close to the 0.2 criterion for open loop resonance. However, $\omega_d > \omega_c$ by a substantial margin. It is concluded that neither closed nor open loop initiated PIO are likely for this configuration. Note that, even with saturated δ_{AS}, there is no resonance to excite PIO.

The rudder dynamics for this case (not shown) permit normal rudder usage, in contrast to the situation at low angle of attack.

D. M2-F3 (SAS ON)

The M2-F2 was modified by adding a fixed center-fin. This decreased the adverse aileron yaw and eliminated the requirement for the ARI. Control authority was doubled. The example, here, is for the same flight condition as the PIO case for flight 16 of the M2-F2.

$$\Delta_{CL}(s) = .949\ [.157,\ .853]\ [.992,\ .456]\ [.685,\ 3.876]$$

$$\frac{\phi}{\delta_{AS}}(s) = 16.496\ (.571)(.612)\ [.343,\ 2.858]/\Delta_{CL}(s)\ \text{(deg/deg)}$$

$$\frac{r}{\delta_{RP}}(s) = -4.962\ (.547)(.571)\ [-.629,\ 1.339]\ (4.296)/\Delta_{CL}(s)\ \text{(deg/sec/deg)}$$

$$\frac{\phi}{\delta_{RP}}(s) = 9.000\ (-8.191)(.571)(.572)(8.084)\ \text{(deg/deg)}$$

$$\omega_c = 3.05$$

$$\measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -150 \text{ degrees}$$

As a first approximation, it is reasonable to expect that limit cycles will exist at frequencies equal to those for the original M2-F2. The increased control authority will reduce the probability that motion amplitudes sufficient to produce a limit cycle will be experienced in flight.

Exercise of the criteria from Figure 5 suggests that PIO is unlikely due to aileron control. However, use of rudder for control of bank or yaw rate can destabilize the system with closed loop bandwidth about equal to 1 radian/second. This could lead to control system saturation and fully developed PIO. A complete, detailed analysis of the nonlinear system would be required to establish the conditions by which PIO could be sustained due to rudder usage.

E. M2-F3 (SAS OFF)

This case was simulated but, apparently, not flight tested. The trim α_0 = -4 degrees.

$$\Delta_{CL}(s) = .949\ [-.575,\ .812]\ [.475,\ 3.251]$$

$$\frac{\phi}{\delta_{AS}}(s) = 16.496\ [.159,\ 2.958]/\Delta_{CL}(s)\ \text{(deg/deg)}$$

$$\frac{r}{\delta_{RP}}(s) = -4.962\ [-.986,\ 1.425]\ (3.629)/\Delta_{CL}(s)$$
$$\text{(deg/sec/deg)}$$

$$\frac{\phi}{\delta_{RP}}(s) = 9.000\ (-8.191)(8.084)\ \text{(deg/deg)}$$

$$\omega_c = 3.39$$

$$\measuredangle\ \frac{\phi}{\delta_{AS}}(j\omega_c) = -164\ \text{degrees}$$

These dynamics are similar to those for the M2-F2 flight 16 PIO case. A detailed analysis would probably indicate that limit cycles are unlikely due to the increased control authority.

The rudder dynamics are poor. Closed loop control of ϕ or r with δ_{RP} will produce closed loop instability. However, control can probably be recovered by:

- ceasing rudder usage, and
- closing the $\phi \rightarrow \delta_{AS}$ loop in a normal manner.

These analytical observations are consistent with the simulator tests of this configuration. Figure 16 shows a system instability

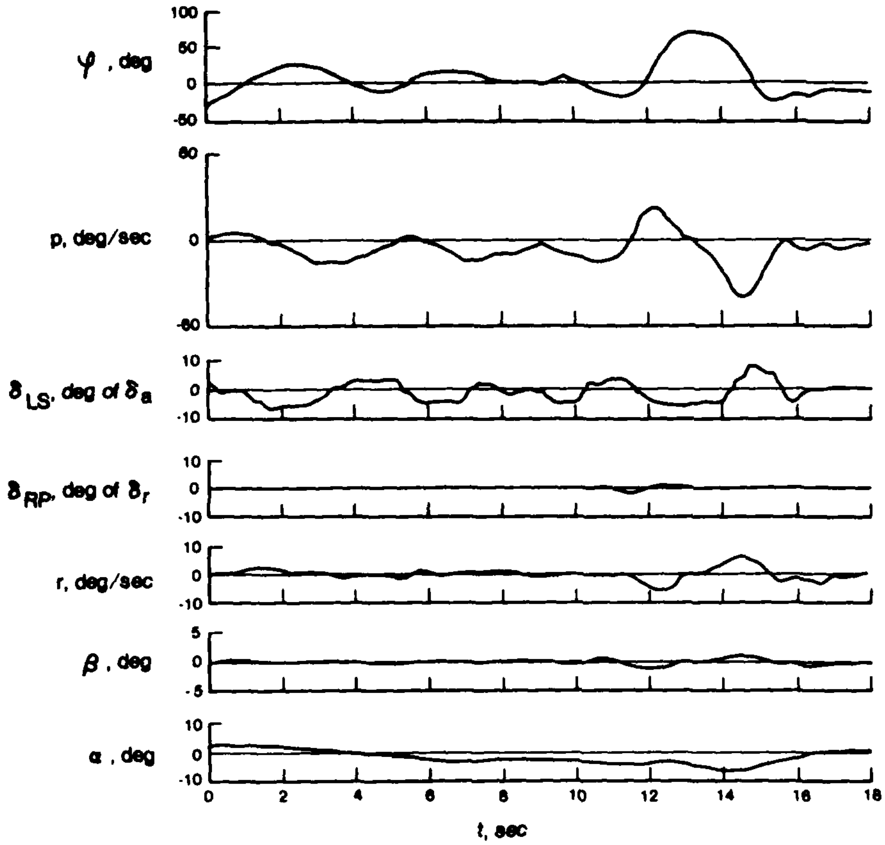


Figure 16. M2-F3 Six-Degree-of-Freedom Simulator Time History of Final Turn and Approach to Landing with SAS Off (from Reference 11, Figure 23)

induced by attempted rudder coordination with aileron. A roll divergence was created; recovery was accomplished in one cycle, without control saturation. The frequency of the large amplitude oscillation is approximately equal to the frequency at which the $\phi \rightarrow \delta_a$ root locus, originating from the unstable roll-spiral mode, crosses into the left-half plane.

F. ESP CONFIGURATIONS

The Equivalent Systems Program (ESP) data was introduced in Section II. The data source was Reference 8. The analysis of these data according to the requirements of Figure 5 was particularly easy. The simulated aircraft dynamics are all linear. Only the aileron control was used. Further, the dutch roll mode was canceled from the roll transfer function. The roll dynamics consisted of conventional spiral and roll subsidence modes plus, for some configurations, a low pass control stick output filter and substantial time delay.

The computed data are shown in Table 1. Since there are neither limit cycles nor resonant open loop dynamics to consider, only the crossover conditions are required for testing the proposed PIO analysis method. The PIO frequencies are shown for the four cases analyzed at McDonnell Aircraft (Reference 8); no time history data were available for the other cases. The Cooper-Harper ratings are shown, including replications.

From Table 1 it may be concluded that the simple phase criterion, evaluated at ω_c, yields very encouraging results. Only configuration L7A shows a clear contradiction between theory and test, PIO predicted but not experienced. However, L7A was only flown once.

For several other configurations for which PIO was predicted to be a problem, but was not found in test, the pilot comments were consistent with those features that could produce PIO in less favorable circumstances. Representative comments are shown in Table 1. Even if these cases prove eventually to be not susceptible to PIO, the phase criterion is still useful since it does indicate configurations which have possible handling qualities deficiencies, even though not of the PIO variety.

TABLE 1. ESP FLIGHT TEST: PIO ASSESSMENT

Conf	ω_c	$\angle \frac{\phi}{F_{AS}}(j\omega_c)$	Prediction: PIO Possible?	Flight Test: PIO Obtained	ω_{PIO}	Pilot Rating
L1	3.99	-168	no	no		4
L2	3.70	-179	marginal	no		3
L3	4.04	-164	no	no		4
L4	3.63	-176	no	no		4
L4A	3.63	-176	no	no		3
L5	3.53	-169	no	no		2/2/2
L6	3.67	-170	no	no		2
L7	3.44	-183	yes	no		3
L7A	3.44	-201	yes	no		4
L8	2.67	-204	yes	yes	2.3-3.14	5
L8A	2.58	-208	yes	(1)		6
L8B	2.48	-212	yes	yes		9/5
L9	3.77	-181	marginal	no		2
L10A	3.77	-192	yes	yes		5
L11/A/B	3.77	-205	yes	(2)		3/4/5/6
L11C	3.77	-226	yes	yes	4.8	9/6
L11D	2.77	-225	yes	yes		10
L12	3.25	-179	marginal	no		4/5
L13	3.16	-186	yes	(3)		4
L14	3.02	-194	yes	(4)		5/7
L14A	2.48	-210	yes	yes		8
L14B	2.12	-222	yes	yes	2.09	8/10
L15	3.34	-192	yes	(5)		4/5
L16	3.34	-202	yes	(6)		3/4
L16A	3.34	-214	yes	yes	3.5	8

Comments:

(1) Overshoots bank angle with 3-4 slow oscillations.

(2) Rated sluggish and sensitive in bank angle control; some requirement for opposite δ_{AS} to stop roll rate; slight overcontrol tendencies.

(3) Oscillated about final bank angle; tended to overturn in sidesteps.

(4) Overshoot desired bank angle, then oscillate; could not bring myself to put in large corrections during sidestep.

(5) Sidestep forced bang-bang control; overcontrolled bank angle.

(6) Overcontrol in sidestep.

G. REENTRY VEHICLES (HARPER)

Simulation of reentry vehicle lateral-directional dynamics in a variable stability aircraft was reported in Reference 14. One hundred and twenty nine configurations were tested. The piloting tasks were of the flight phase Category B type, plus IFR tracking. One evaluation pilot was used; only three repeat runs were made. The primary purpose of the test was to accumulate data for the general evaluation of lateral-directional handling quality parameters such as ω_ϕ/ω_d and $|\phi/\beta|$. PIO was not specifically sought and, in fact, was only found in two cases (configurations 94 and 102). For configuration 90, undamped roll oscillations were found during IFR tracking which were not a problem in VFR control. Consider each of these three configurations:

1. Configuration 90:

$$\omega_c = 3.58 \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -160$$

No PIO problem is indicated in closed loop roll control. The dutch roll mode damping ratio, ζ_d, is .268; the mode is not resonant according to the definition used here. Note, however, that $\measuredangle\ \phi/\delta_{AS}(j\omega_d) = -179$. It is not inconceivable that due to instrument lags or limited ϕ-resolution, either the open loop dutch roll oscillations did create an IFR control problem or the resultant phase angle at ω_c violated the -180 degrees criterion. This is conjectural, but not an unreasonable account of the differences observed between VFR and IFR flight.

2. Configuration 94:

$$\omega_c = 3.43 \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -149$$

No PIO problem is indicated at the predicted crossover frequency. The dutch roll mode is resonant ($\zeta_d = .04$).

$$\omega_d = 4.55 \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_d) = -230 \text{ degrees}$$

This indicates a possible PIO problem at the dutch roll frequency.

3. Configuration 102:

The results of this case are very similar to the results for configuration 94:

$$\omega_c = 2.88 \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -147$$

$$\zeta_d = .15$$

$$\omega_d = 4.15 \qquad \measuredangle \frac{\phi}{\delta_{AS}}(j\omega_d) \approx -215$$

According to the proposed theory, there are several other configurations that would have been designated as possibly PIO-prone. These are: 54, 77, 78, 85, 86, 92, 93, 101, and 105. These were not identified as PIO-prone in flight test. Whether this was due to insufficient testing, to test technique, or to a genuine lack of PIO problems cannot be determined. It is interesting that for all these 9 configurations, except 92, $\omega_\phi/\omega_d > 1$. The pilot ratings were 6-9 for all cases except 54, 85, and 105.

It is tentatively concluded that the PIO analysis method of Figure 5 gives realistic indications of PIO problems, specifically, and potential handling problems for most of the 129 cases tested.

H. THE PRINCETON DATA

Reference 15 documents flight tests of 134 lateral-directional dynamic configurations simulated with the Princeton Navion. Perhaps what this report shows more than anything else is the total breakdown of the empirical approach to handling qualities when applied to the study of lateral-directional dynamics. There are too many parameters and their interactions are far too complex for any practical test program to deal with in a strictly empirical manner. The authors noted, for example, the inability to independently vary L_β and $|\phi/\beta|$ over a useful dynamic range. One interesting case for PIO research was encountered.

Configuration 81 was found to have fair handling qualities in the approach to carrier landing task (R = 4.5, Cooper scale). No actual touchdowns were made, nor was flare control a requirement. However, this same configuration demonstrated PIO problems in nontracking flight tasks (Category B). For the configuration 81 dynamics:

$$\omega_c = 3.24$$

$$\measuredangle \frac{\phi}{\delta_{AS}}(j\omega_c) = -151$$

$$\zeta_d = .1$$

$$\omega_d = 1.8$$

$$\measuredangle \frac{\phi}{\delta_{AS}}(j\omega_d) = -199$$

These data are consistent with the flight test results. No PIO problems in tracking are indicated. The dutch roll mode is resonant, and a possible PIO problem at ω_d is indicated.

I. CONCLUSIONS

Almost all of the data considered support the proposed PIO assessment method of Section IV. In those cases where the method indicated possible PIO problems to occur, but where none were found in flight test, the handling qualities were nearly always poor, nonetheless.

SECTION VI
CONCLUSIONS AND RECOMMENDATIONS

Single axis, roll-only PIO appears to be possible. That is, lateral-directional PIO does not necessarily require simultaneous pilot control of multiple cues (e.g., ϕ and r) or the use of more than one control. While PIO may, in some cases, be initiated through such mechanisms, the data used in this study indicate that possible PIO problems can generally be forecast based on $\phi \rightarrow F_{AS}$ dynamics.

The method proposed for the assessment of lateral-directional PIO is very similar to traditional methods. It differs mainly in the quantitative prescription of specific frequencies for exercise of stability criteria (linear or limit cycle).

It isn't clear whether lateral-directional PIO is inherently "different" from longitudinal PIO or not. The longitudinal theory (Reference 1) hypothesized a coupling between the pilot's control of pitch attitude and normal acceleration as the source for PIO difficulties. During the present work, an attempt was made to extend the longitudinal theory to the lateral-directional case. There is some weak evidence that lateral acceleration may be a factor in some lateral-directional PIO. Unfortunately, lateral acceleration data are almost never reported in the handling qualities literature. Because of this, it wasn't possible to pursue this explanation for PIO. However, in the spirit that the simplest theory is the best, the roll-only PIO model may be satisfactory until new data suggests otherwise.

One area where lateral acceleration may play a role is with direct side force control applications. There are no data known to this author about PIO with such flight control systems. Still, it is reasonable to assume that PIO could occur in tasks such as ground attack or level turns for bombing. If so, the longitudinal theory may apply, directly, provided lateral acceleration and an appropriate outer loop (guidance) cue--which is task dependent--are used in place of normal acceleration and pitch attitude.

The apparent success of the roll-only PIO model suggests that the pitch PIO theory should be revised to account for a single axis pitch attitude PIO. The assessment method would be identical with that developed in this report for roll PIO. A corollary to this single axis pitch PIO mode is that PIO can be obtained in fixed base simulation which is physically equivalent to that found in flight test. It is noted, however, that in the absence of system nonlinearities, flight control system time delays, or higher order system dynamics, the stability criterion $\measuredangle\ \theta/F_{ES}(j\omega_c) > -180$ degrees will almost always be satisfied; thus single axis pitch PIO probably can't occur unless one or more of these effects are present. This is not the case with the original theory of Reference 1. In other words, the classic airplane can, according to Reference 1, exhibit PIO due to coupling, through pilot control, of pitch and normal acceleration. However, by the theory of this report, it would be unlikely to have a pitch-only PIO.

It is recommended that future flight tests, conducted for the purpose of extending the PIO data base (longitudinal or lateral-directional), should be done using direct force control techniques to decouple attitude and linear acceleration modes. It should be possible to identify the various effects on PIO or handling qualities when the testing is done in this way. There is a certain urgency to accumulate data of this sort in view of the flight control system state of the art.

No revisions to MIL-F-8785C are recommended based upon analyses in this report. More handling qualities data are required from flight tests dedicated to testing proposed criteria before such proposals can be made--if, indeed, they are warranted.

APPENDIX

HANDLING QUALITIES PREDICTION IN APPROACH AND LANDING

Reference 16 documents one of the more recent and important handling qualities data bases to become available. Longitudinal dynamics were varied over a broad range and simulated with the variable stability T-33. The piloting task was landing approach, with actual touchdowns in most cases. This data set is identified by the acronym LAHOS (Landing Approach Higher Order Systems).

Longitudinal PIO in the approach and landing task were specifically sought as a subset of the LAHOS experiment. Several were found. This data set is important to the present work because it increases the general knowledge of the PIO problem, and permits an independent check on the handling quality parameters proposed in Reference 5. Of the latter, the parameters $\measuredangle\, \theta/F_{ES}(j\omega_c)$ and ω_c are the most important. The other is the normal acceleration phase parameter:

$$\Phi\,(j\omega_c) = \measuredangle\, \frac{a_{n_p}}{F_{ES}}\,(j\omega_c) - 14.3\,\omega_c$$

The parameters ω_c and $\measuredangle\, \theta/F_{ES}(j\omega_c)$ are of central importance to the method for PIO assessment proposed in the main body of this report.

It is noted that these two parameters have already been evaluated for the LAHOS data set (in Reference 17). The calculation of ω_c was made using the original formula (Reference 5). Here, the slightly modified version of the crossover frequency formula from Section II is used:

$$\omega_c = 6.0 + 0.24 \times S$$

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where

S = average slope (in db/octave) of $|\theta/F_{ES}(j\omega)|$ on $1 \leq \omega \leq$ 6 radians/second

In fact, this formula was developed, in part, from inspection of the LAHOS data. However, no attempt was made to optimize the ω_c formula in any sense. The use of the modified formula yields significantly better correlation between pilot rating and $\measuredangle\, \theta/F_{ES}(j\omega_c)$ and generally appears to be consistent with observable closed loop oscillation frequencies.

This correlation is shown for all LAHOS configurations in Figure A1. The empirical rating boundaries from Reference 5 are superimposed on the data points (these are the same boundaries shown in Figure 4, Section IV).

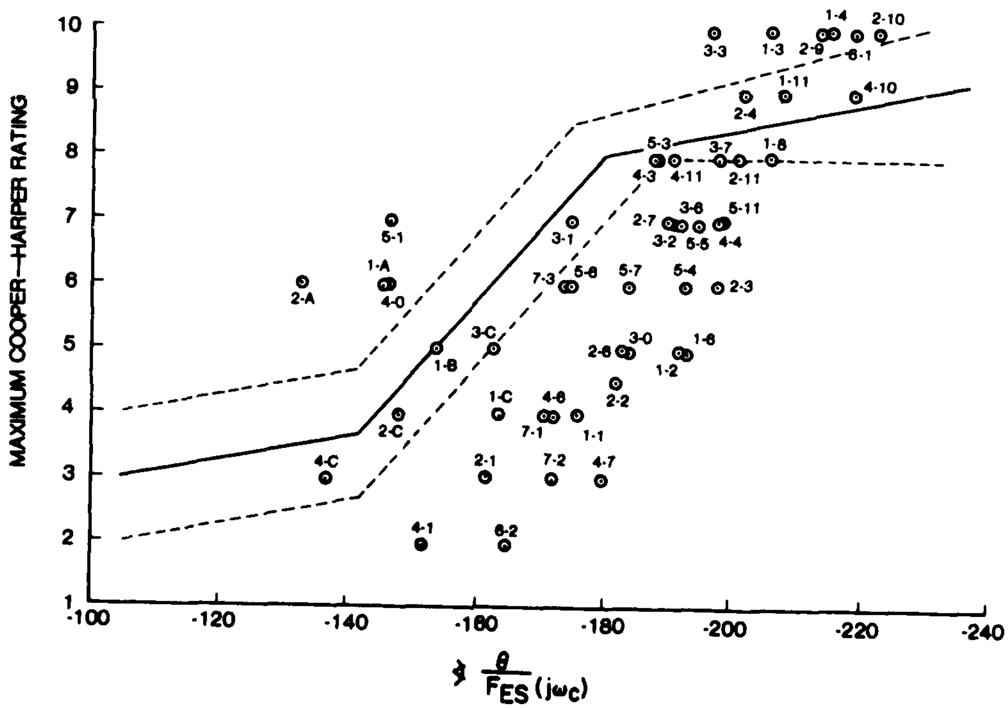

Figure A1. Correlation of LAHOS Data with $\measuredangle\, \theta/F_{ES}(j\omega_c)$

With the exception of configurations 1-A, 2-A, 4-0, and 5-1, all the other data points show consistent variation with $\measuredangle\ \theta/F_{ES}(j\omega_c)$. The spread in the data at fixed $\measuredangle\ \theta/F_{ES}$ is about 2 1/2 Cooper-Harper units--over twice that of the same fit achieved for the Neal-Smith data (Reference 18) analyzed in Reference 5 and shown in Figure 4.

Reference 5 proposed a handling qualities criterion based on the normal acceleration phase angle parameter $\Phi\ (j\omega_c)$, as follows:

Level 1: $\Phi \geq -160$ degrees for any value of $\measuredangle\ \theta/F_{ES}(j\omega_c) \geq -130$ degrees, or any Φ when $\measuredangle\ \theta/F_{ES}(j\omega_c) \geq -122$ degrees

Level 2: $\Phi \geq -220$ degrees for any value of $\measuredangle\ \theta/F_{ES}(j\omega_c) \geq -165$ degrees, or any Φ when $\measuredangle\ \theta/F_{ES}(j\omega_c) \geq -148$ degrees

Level 3: otherwise

When these criteria are applied, it can be shown that:

- Configuration 2-A is predicted to be a definite Level 2 condition: $\measuredangle\ \theta/F_{ES}(j\omega_c) = -133$ degrees, $\Phi\ (j\omega_c) = -204$ degrees.

- Configurations 1-A, 4-0, and 5-1 are all predicted to be borderline Level 2/3 since, in each case, $\measuredangle\ \theta/F_{ES}(j\omega_c)$ is within 2 degrees of the borderline value (-148 degrees).

It, therefore, appears that these four cases are entirely consistent with the handling quality assessment rules developed in Reference 5 based on air combat type control tasks.

Of those LAHOS configurations which obey the rating variation from Reference 5, only 4-C would be misrated by imposing the $\Phi\ (j\omega_c)$ criteria. For 4-C, $\Phi\ (j\omega_c) = -225$ degrees and $\measuredangle\ \theta/F_{ES}(j\omega_c) = -137$ degrees. According to the cited criteria, this configuration should also have been Level 2 (i.e., similar to the other 4). This configuration was flown twice. In both instances, the pilot complained of pitch bobble and some abruptness in initial pitch response.

There are 23 configurations that lie significantly below the Category A boundaries from Reference 5. There are several possible explanations for this:

1. The $\angle\, \theta/F_{ES}(j\omega_c)$ parameter is invalid.

2. The $\angle\, \theta/F_{ES}(j\omega_c)$ parameter does not apply to the approach and landing task.

3. There are additional (but unknown) handling quality metrics which will unify the two data sets.

4. There were systematic differences between the processes by which the two data sets were collected, and this resulted in skewing of the one set relative to the other.

5. The process by which one, or both, data sets were collected was faulty.

Considering the self-consistency of the LAHOS data when correlated with $\angle\, \theta/F_{ES}(j\omega_c)$ [with necessary allowance for $\Phi\,(j\omega_c)$] and its successes elsewhere, the first two possibilities are difficult to accept. The third possibility is impossible to evaluate; there are undoubtedly other metrics which could be correlated with some or all of these data. Time response calculations with the LAHOS configurations suggest that a rise-time criterion on pitch rate has some merit, for example. The fifth possibility will be discounted out-of-hand.

There were at least two possible effects which may have produced systematic differences between the Neal-Smith data set and the LAHOS data. The most apparent difference is in the nature of the tasks performed by the evaluation pilots. One was terminal, the other was not. A second, but related, effect may have been approach and landing performance, and its effect on the handling qualities evaluations. This is one of the grey areas of present-day handling qualities research. Qualitative evidence suggests that good performance in a manual control task can result in substantially improved pilot ratings when compared with nominal rating data.

It is not entirely clear how an evaluation pilot determines control performance in a landing task. The really poor configurations in Category A tasks probably tend to be poor in the landing task as well. This may account for the consistency observed between the LAHOS data and the Reference 5 boundaries in the Level 3 region. In Category A (tracking) tasks, pitch attitude provides a continuous display of control performance; there is no ambiguity about performance of the control task. Although there are physical cause and effect relations between attitude variations and touchdown conditions in the power approach, the relations are not necessarily clear to an evaluation pilot, moment-to-moment.

The use of Cooper-Harper ratings in the terminal control task may also be questioned. The scale is defined in terms of "workload" and "performance." In an approach, the pilot workload may be very low, even with degraded handling qualities, provided the aircraft is trimmed at an acceptable, nominal state of motion. In flare, the workload may be intense for a few moments. It isn't clear how a pilot can construct a rational rating when:

- the approach control was nearly nominal, without significant disturbances,
- a brief period of high workload was experienced in flare and postflare due to unanticipated pitch overshoots which excited undesirable heaving motions,

- with a few well-timed control inputs, the undesirable motions were arrested, and

- the aircraft was landed safely, within nominals.

Based mainly on pilot comments from Reference 16, it appears that scenarios such as this were not uncommon during the LAHOS trials. The resulting Cooper-Harper rating may very well depend upon how much the pilot blames himself for the unanticipated response, or upon how smooth his control inputs were. Because of considerations of this type, it is conceivable that aircraft having very poor handling in Category A tasks could be rated very favorably in the approach and landing task with the same dynamics. In such cases, however, one may expect that eventually the situation will occur when the pilot will excite the undesirable responses.

This jaundiced view is supported by extensive fixed-base simulator results. Aircraft dynamics similar to those of a current ground attack aircraft were simulated. A terrain board-TV projection visual display was used. The aircraft dynamics were modified by adding time delay increments to the solutions of the aircraft equations of motion. Since time delay has no effect on the computed crossover frequency, ω_c, all of the pilot rating data obtained as a function of time delay were for constant ω_c. The resulting correlations between ratings and $\measuredangle\, \theta/F_{ES}(j\omega_c)$ are shown in Figure A2 for two tasks: approach and landing (the circles) and ground attack (the filled triangles). All of the data for 4 test pilots are shown for six values of time delay. The rating boundaries derived in Reference 5 are shown on the figure, also.

Comparison of Figures A1 and A2 suggests that the simulator data for the approach and landing task is qualitatively similar to the LAHOS data--especially for $\measuredangle\, \theta/F_{ES} > -200$ degrees. The simulated ground attack task, however, was substantially more difficult than the landing task, with the exception that for very large lags in $\measuredangle\, \theta/F_{ES}$,

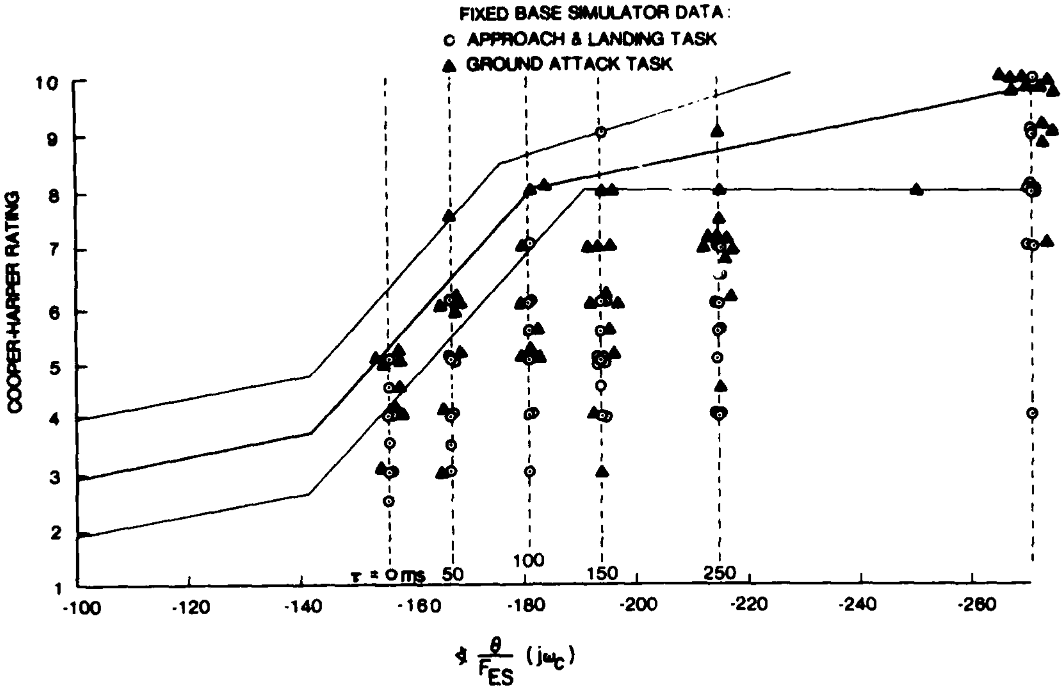

Figure A2. Correlation of Fixed Base Simulator Data with $\measuredangle\ \theta/F_{ES}(j\omega_c)$

both tasks were often rated poorly. Figure A3 is a plot of the ratings (averaged at each value of delay) versus delay for the two tasks in Figure A2 plus a terrain following task.

On the basis of average pilot ratings, the ground attack task is clearly the most difficult. However, the ratings appear to originate from a statistical process. On occasion, it is expected that ratings in the approach and landing task will equal the average rating found in the Category A task--even though there are significant differences between the average ratings for the two tasks when sufficient rating data have been collected. It was also determined (qualitatively) that in the approach and landing task those flights for which the pilot ratings were very optimistic were also those for which task performance was very good with minimum control activity by the pilot. For these cases, it appears that the pilot was able to establish a trimmed, nominal approach which required minimum workload. This even

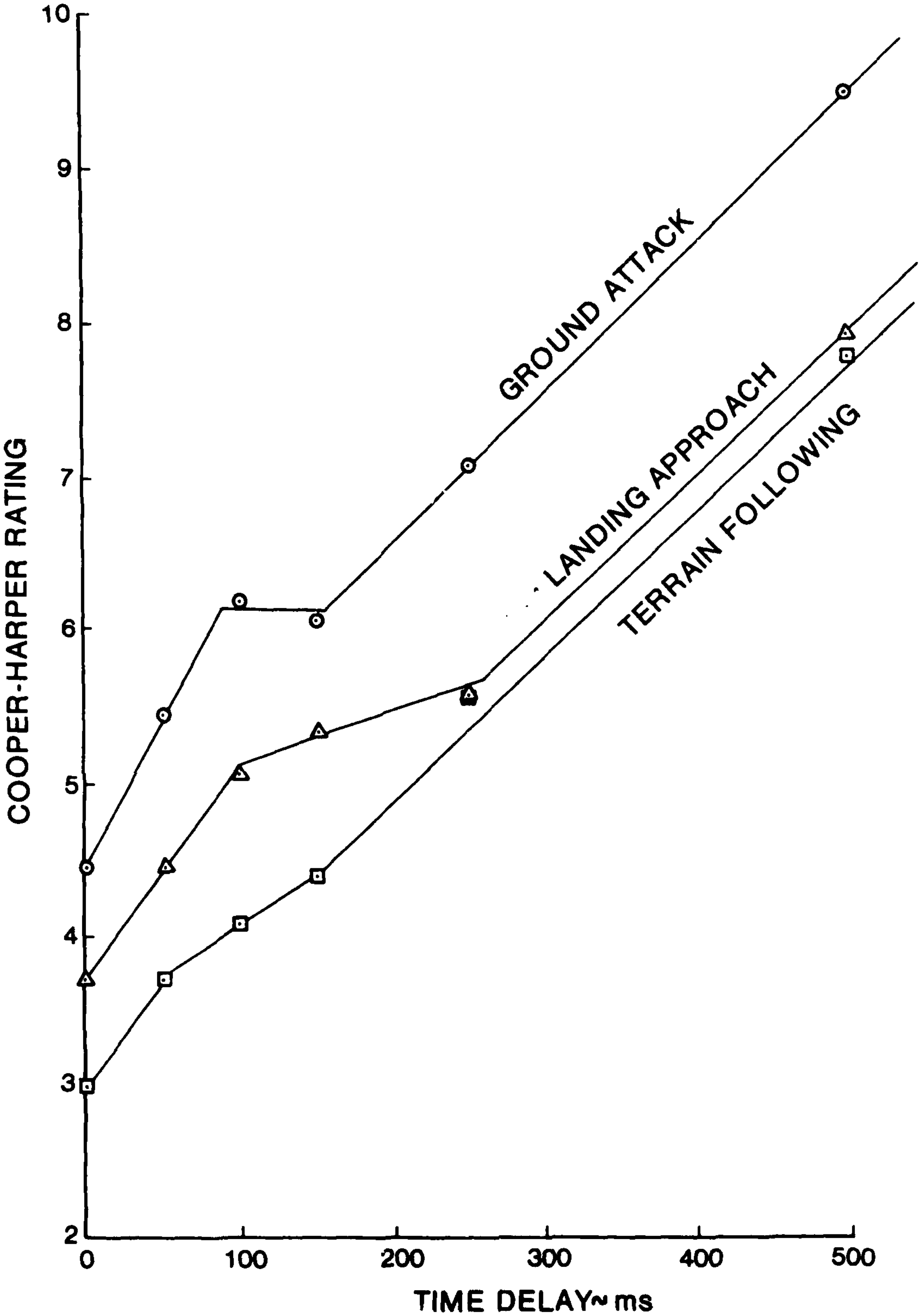

Figure A3. Raw Data: Longitudinal Task Comparisons
Averaged Pilot Data

occurred with configurations that were basically unflyable in the ground attack task. It would be interesting to determine for the LAHOS data whether comparable situations existed. This might be done by spectral analysis of the pilot's control response to determine whether periods of nontracking control existed.

One conclusion that may be drawn from this comparison is that it may be unconservative to base handling quality design specifications for approach and landing on data obtained from approach and landing testing--at least not without exercising some very critical judgments. In view of the statistical nature of the landing accident, we ought to be more skeptical about the assumption that "looser," more liberal design criteria are satisfactory for Category C tasks than would be the case for Category A.

It is recommended that these speculations be tested, first in a moving-base simulator and, if warranted, later in flight. It would be of great value to determine whether the LAHOS configurations would be given ratings consistent with those of the Neal-Smith configurations when comparable tasks were flown. It is also desirable that the ability of the parameters ω_c, $\angle\, \theta/F_{ES}(j\omega_c)$ and $\Phi\ (j\omega_c)$ to unify a spectrum of precision tracking data (longitudinal) be fully tested.

Until it is proven otherwise with suitable testing, it will remain the opinion of this author that the approach and landing data of Reference 16 generally follows the variations derived in Reference 5 based on Category A data. Those data from Reference 16 that differ substantially from the boundaries derived in Reference 5, with $\angle\, \theta/F_{ES}(j\omega_c)$ as the principal correlator, reflect the peculiarities of obtaining subjective opinion ratings in terminal control tasks.

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